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The Proceedings

OF

THE INSTITUTION OF ELECTRICAL ENGINEERS

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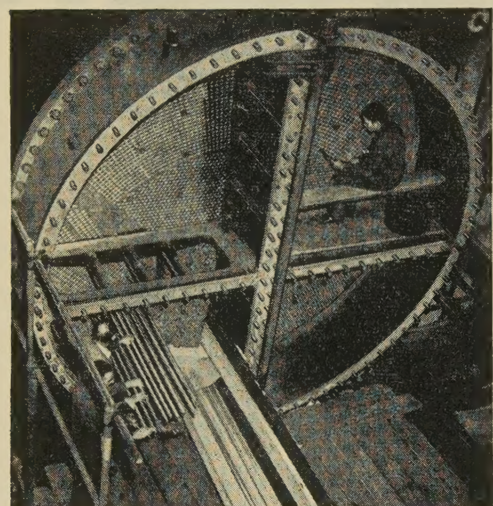

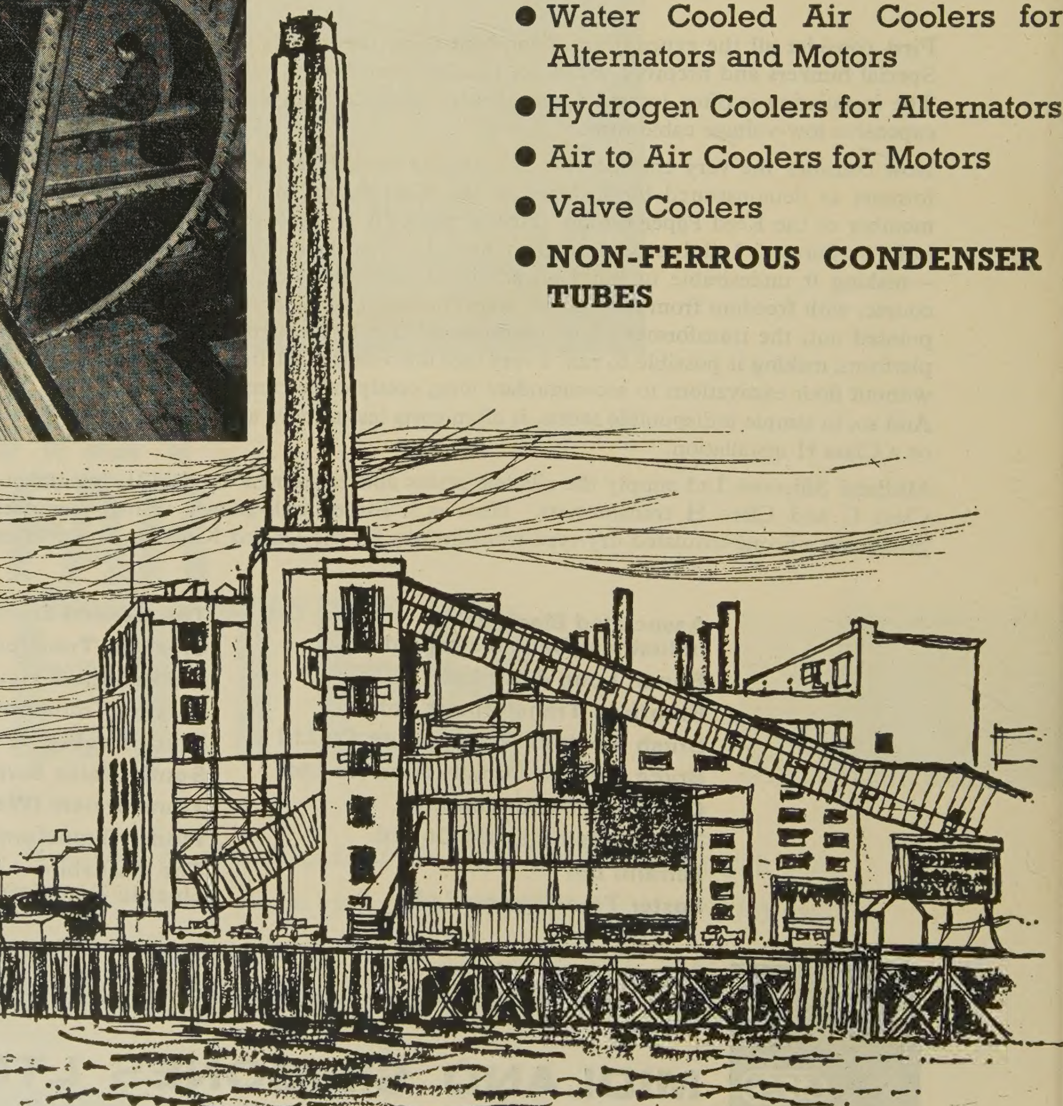

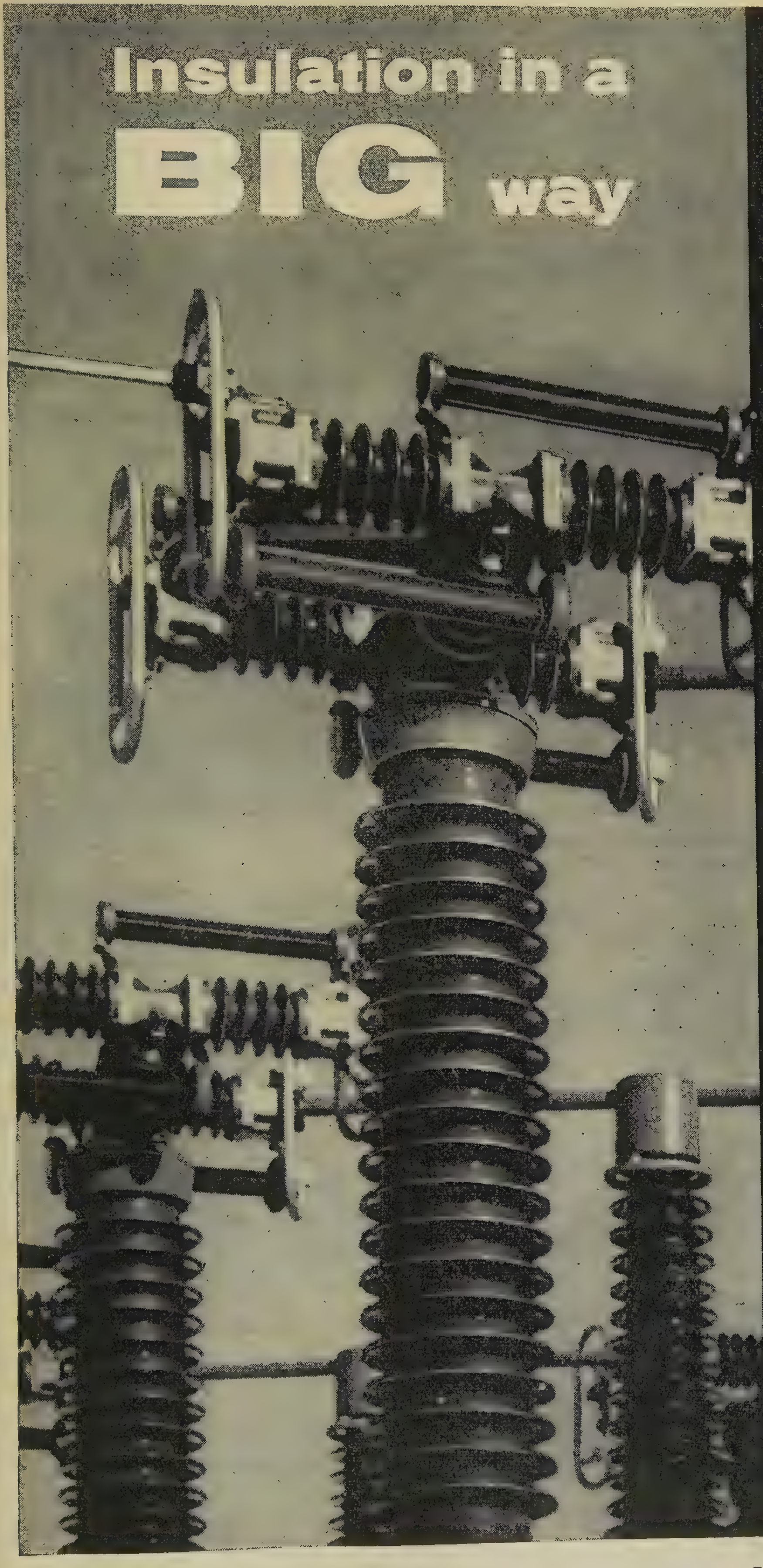
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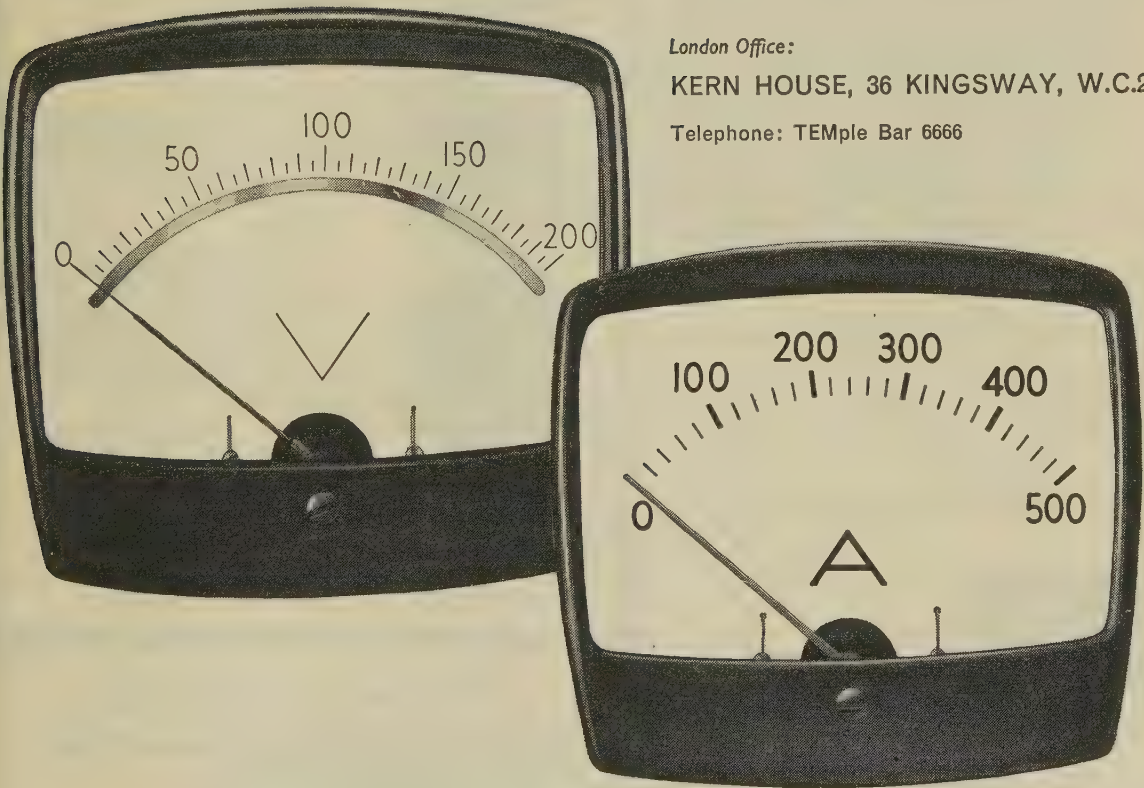

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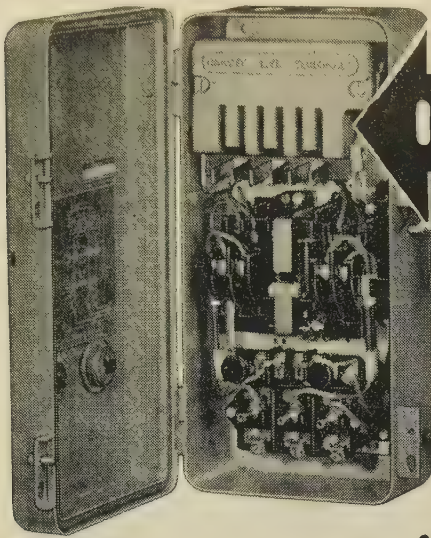

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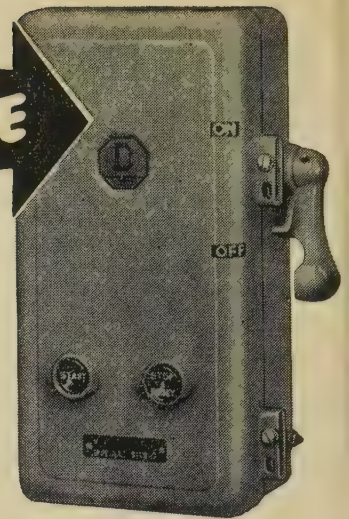

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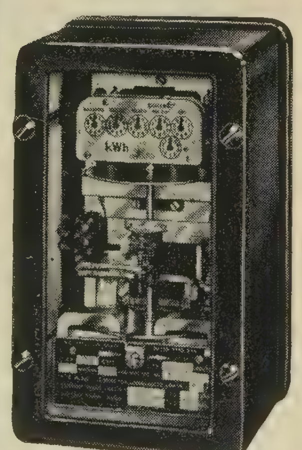

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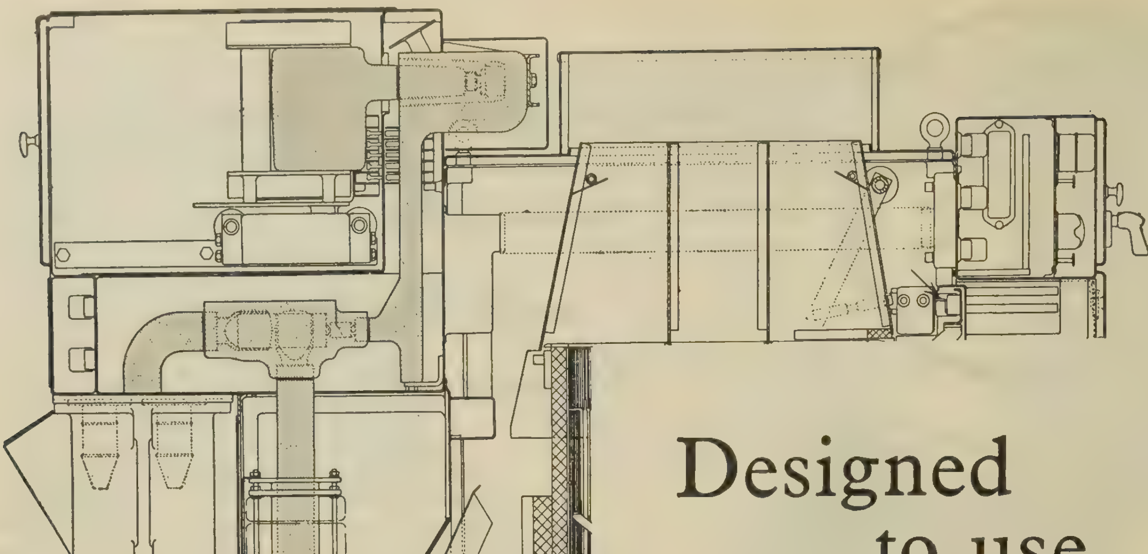

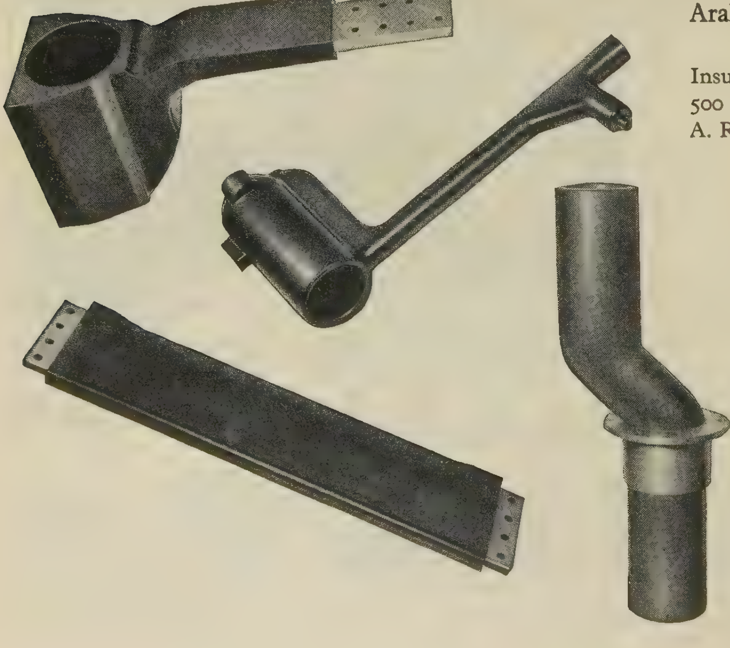

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OPEN-CIRCUIT NOISE IN SYNCHRONOUS MACHINES

By J. H. WALKER, Ph.D., M.Sc., Member, and N. KERRUISH, M.A., Associate Member.

(*The paper was first received* 9*th April, and in revised form* 12*th July*, 1960.)

SUMMARY

In order to predict the noise level of a synchronous machine the paper develops equations to represent the permeance and m.m.f. variation at the stator bore. From these two equations expressions are derived which give the force variation causing vibrations in the core. The method for calculating the amplitude of, and number of nodes associated with, any particular harmonic is given. From this the loudness of the noise in decibels is obtained. Six examples are given of the application of the method to actual machines, on five of which the comparison is made between the results predicted by calculation and those obtained in service. Good agreement is obtained between calculation and observation, and it is thus possible to draw certain conclusions concerning the factors which influence the generation of open-circuit noise, in particular the objectionable noise at about slot frequency in synchronous machines. The paper shows that there are six such factors, namely

(*a*) The stator permeance harmonics caused by the presence of the stator slots.
(*b*) The rotor m.m.f. harmonics caused mainly by the shape and configuration of the rotor poles.
(*c*) The pole pitch of force harmonics.
(*d*) The frequency of the noise which depends on the number of poles and the number of slots.
(*e*) Peripheral velocity of the noise harmonic.
(*f*) Moment of inertia and natural frequency of the stator core.

For noise to be generated, factors (*a*) and (*b*) must both be present in combination with one or more of the remaining factors. If required the method can also be used to calculate the noise spectrum over a range of frequencies. It is shown that, theoretically, skewing of the stator or rotor may reduce or eliminate open-circuit noise in a machine, but that practical difficulties restrict its field of application.

LIST OF SYMBOLS

B = Flux density at stator bore, p.u.
D_c = Outside diameter of stator core, in.
D_g = Inside diameter of stator core, in.
d = Depth of stator slot, in.
δ = Amplitude of static deflection of core, in.
E = Young's modulus, lb/in^2.
F = Force wave at stator bore, p.u.
f = Line frequency, c/s.
f_h = Frequency of harmonic, c/s.
f_0 = Natural frequency of stator core in required mode of vibration, c/s.
g = Radial length of air-gap, in.
I = Moment of cross-sectional area of core, in^3.
K_n = Amplitude of nth harmonic in m.m.f. wave, p.u.
k_1 = Modifying factor corresponding to $v/1\,125$.
k_2 = Modifying factor for natural frequency of core vibration.
K_{nm} = Amplitude of harmonic in flux density wave, p.u.
$K'_{n'm'}$ = Amplitude of harmonic in force wave, p.u.
L_m = Amplitude of mth harmonic in permeance variation, p.u.
L_c = Length of stator core, in.
M = Rotor m.m.f. at stator core, p.u.
m = Order of permeance harmonic.
m' = An integer, odd or even.
N = Angular velocity of rotor, r.p.m.
n = Order of m.m.f. harmonic (odd integer).
n' = An integer, odd or even.
$\mathscr{P}$ = Permeance at stator bore, p.u.
p = Number of pairs of poles of machine.
q = Number of stator slots.
r_1 = Angular velocity of force harmonic, rad/s.
r = Harmonic pole-pitch/fundamental pole-pitch.
s = Stator slot width, in.
t = Time, sec.
θ = Angle measured relative to position on stator bore, rad.
v = Peripheral velocity of harmonic wave, ft/s.
1125 = Velocity of sound in air at N.T.P., ft/s.
W = Width of rotor pole shoe, in.
F_l = Force per unit axial length of core, lb/in^2.

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.

Dr. Walker and Mr. Kerruish are with Associated Electrical Industries (Rugby), Ltd.

(1) INTRODUCTION

A long-standing problem in the design of synchronous machines has been the prediction of the open-circuit magnetic

noise.* This noise usually occurs in machines with fractional numbers of open stator slots per pole, since during the movement of the rotor through one stator slot pitch there is a relatively large variation in the air-gap flux density. This variation may produce a single penetrating note at approximately slot frequency, i.e. 500/1 500 c/s, which is highly objectionable from the point of view of operating staff, and its avoidance thus constitutes a serious design problem. A number of contributions have been made towards the solution of this problem, the earliest being that by Graham, Beckwith and Milliken[4]. In all but one of these papers the investigations are largely empirical and permit little more than the prediction of the frequency and number of nodes of the force wave most likely to produce noise. The only paper in this field to give a mathematical analysis of all the relevant factors and to permit an assessment of the likelihood of objectionable noise is that of Carter.[1] Here, however, in striving for extreme accuracy, Carter treated the air-gap flux density as a vector instead of a scalar. The resulting analysis is so recondite that it not only obscures the significance of the various factors in the design of a machine which influence the generation of noise but also leads to such complex equations that their numerical solution by hand is impracticable and even with a computer would be uneconomic. Finally, largely owing to a lack of experimental data—which, however, are now available[12]—concerning the behaviour of stator cores under the influence of high-frequency low-amplitude force waves, Carter arrived at noise energy figures which, whilst permitting a comparison between similar machines, bore little relation to the observed values of noise.

By a simple application of conformal transformation and Fourier analysis, the present paper determines the amplitudes of the harmonics in the expressions for the stator permeance and rotor m.m.f. waves. From these expressions are deduced the fundamental equations governing the modes of vibration of the stator core. By the use of these equations, with certain simplifying assumptions involving a negligible reduction in accuracy, a method is developed for calculating the machine noise level in phons for the harmonic in question. Since the analysis is general it may be used to calculate the noise spectrum over a range of frequencies and thus the total noise.

(2) DERIVATION OF HARMONICS IN MAGNETIC FORCE WAVE

The magnetic force acting at any point on the stator bore is proportional to the square of the flux density at that point. Owing to the effects of differential fringing on the flanks of the teeth the flux density has both a radial and a tangential component, i.e. B should be treated as a vector. However, the deflection of the core due to the tangential component will always be very small compared with that of the radial component and will therefore be neglected here; B will thus be treated as a scalar acting radially.

The m.m.f. due to the rotor winding acting on a smooth unslotted stator bore will vary generally as shown in Fig. 1, and may be represented by

$$M = \sum_{n=1,3} K_n \cos [np(\theta - \omega t)] \quad . \quad . \quad . \quad . \quad (1)$$

With normal slotting and on the assumption of a uniform m.m.f. acting across the gap, the variation in the permeance at the stator bore will be as shown in Fig. 2. This permeance wave may be represented by

$$\mathscr{P} = \sum_{m=0,1,2} L_m \cos mq\theta \quad . \quad . \quad . \quad . \quad . \quad (2)$$

* Although this analysis assumes the machine is on open-circuit, experience shows that the noise persists unchanged on load.

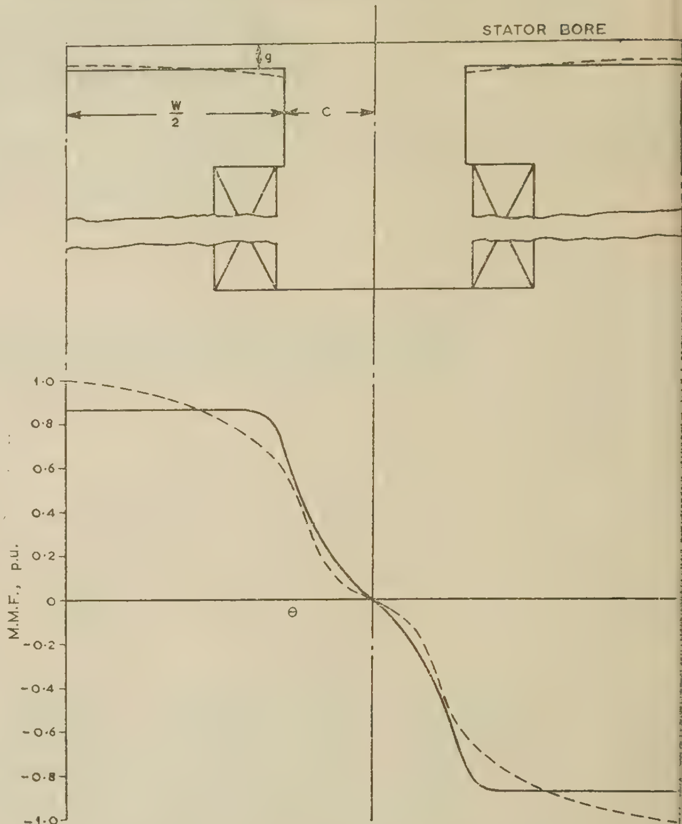

Fig. 1.—Rotor poles opposite smooth stator and corresponding m.m.f. curves.
——— Parallel air-gap.
– – – – Shaped air-gap.

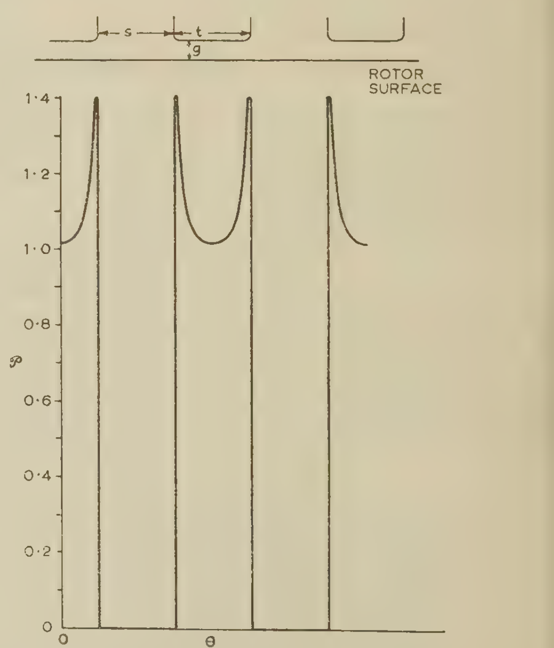

Fig. 2.—Slotted stator opposite smooth rotor and corresponding permeance variation with uniform m.m.f. acting across the air-gap.
Permeance over the slot produces mainly tangential flux at the flanks of the teeth; it is therefore taken as zero.

To calculate the flux density due to an m.m.f. varying as in eqn. (1) and acting on a slotted core as in Fig. 2, the assumption is made that the permeance at any point [eqn. (2)] remains the same with a non-uniform m.m.f. as when the m.m.f. is uniform. This assumption is not strictly valid, but criticism can best be met by the comment of Adkins:[2] 'This assumption is not based on any logical argument and its validity can be tested only by whether the deductions agree with practical results.' Since then this assumption has been used successfully in a number of investigations of machine performance.[3, 7] Therefore, to obtain the flux density, eqns. (1) and (2) are multiplied together to give

$$B = \Sigma \Sigma K_n \cos [np(\theta - \omega t)] L_m \cos mq\theta \quad . \quad (3)$$

Eqn. (3) can be written in the simpler form:

$$B = \Sigma \Sigma K_{nm} \cos [(np \pm mq)\theta - np\omega t] \quad . \quad . \quad (4)$$

The force acting on the stator bore due to the flux density B is, of course, directly proportional to B^2, i.e.

$$F = \{\Sigma \Sigma K_{nm} \cos [(np \pm mq)\theta - np\omega t]\}^2 \quad . \quad . \quad (5)$$

Since this expression is the square of a sum of terms, it is the sum of squares such as $\cos^2 [(np - mq)\theta - np\omega t]$ and products such as $\cos [(n_1p \pm m_1q)\theta - n_1p\omega t] \cos [(n_2p \pm m_2q)\theta - n_2p\omega t]$. A product of cosines can be expressed as a sum of cosines and the square of a cosine can be expressed in terms of the cosine of the double angle: therefore both the above expressions consist of such terms as $\cos \{[(n_1 + n_2)p + (m_1 + m_2)q]\theta - (n_1 + n_2)p\omega t\}$.

Since n_1 and n_2 are both odd integers, $n_1 + n_2$ is even and may therefore be called $2n'$, where n' is an integer which may be odd or even. Also $m_1 + m_2$ may be called m' and may be odd or even. Hence, with no loss of generality, eqn. (5) may be simplified to

$$F = \Sigma \Sigma K'_{n'm'} \cos [(2n'p \pm m'q)\theta - 2n'p\omega t] \quad . \quad (6)$$

With a knowledge of the amplitudes of the rotor m.m.f. harmonics (corresponding to $n'p$) and those of the permeance harmonics (corresponding to $m'q$), it is possible to determine from eqn. (6) the force waves which are liable to produce noise in a given machine.

(3) FORCE HARMONICS LIABLE TO PRODUCE NOISE

In spite of its simplicity, eqn. (6) contains most of the information required to assess the danger of any given force harmonic producing noise.

(3.1) Pole Pitch of a Harmonic Force Wave

The coefficient of θ in eqn. (6) defines the pole pitch of a force wave. A wave corresponding to the plus sign deflects the stator ring into a large number of nodes, and that corresponding to a minus sign, into a small number. It thus follows that the deflection of the stator core will be much greater in the latter case than in the former. For this reason the waves corresponding to the plus sign in the expression $(2n'p \pm m'q)$ will be omitted from the subsequent analysis and attention concentrated on the $(2n'p - m'q)$ coefficient; the most dangerous condition is likely to be that corresponding to a small value of $2n'p - m'q$.

The pole pitch of a harmonic, which is inversely proportional to $(2n'p - m'q)$, is not in itself a guide to the level of noise but must be related to the main pole pitch of the machine. Thus, in the case where $2n'p - m'q = 1$, which defines the harmonic with the longest pole pitch, noise is not likely in, for example, a 4-pole machine, since the pole pitch of the harmonic is only twice that of the main poles. If, however, this condition arises in, say, a 26-pole machine, noise is likely since the pole pitch of the harmonic is now 13 times that of the main poles.

It is found in practice that the harmonic with $m' = 1$ and n' equal to the nearest integer to slots per pole is the one most likely to produce noise. The ratio of the main pole pitch to the pole pitch of this harmonic is given by twice the ratio of the numerator to the denominator of the fractional part of $q/2p$. The smaller this ratio the greater is the danger of noise.*

Test measurements have shown that the frequency of this predominant noise is usually given by the following empirical rule:[4]

$$f_h = 2 \times \text{line frequency} \times \text{nearest integer to slots per pole}$$

The theoretical justification for this rule (see Section 10.1) is obtained by determining the value of n' which makes $(2n'p - m'q)$ [eqn. (6)] a minimum, i.e. corresponding to the wave of longest pole pitch with $m' = 1$. This value of n' is the integer nearest to $q/2p$, i.e. the integer nearest to the number of slots per pole. Then, from eqn. (6), the frequency of the noise is $2n'f$. In general, for any values of n' and m' the minimum value of $2n'p - m'q$ is given[5] by the highest common factor of $2p$ and q.

It may be noted here that the frequency of the noise harmonic is given by the time coefficient of eqn. (6), i.e. $2n'p\omega = 2n'f$.

Table 1

POLE PITCHES OF HARMONIC FORCE WAVES REPRESENTED BY $\cos [(2n'p - m'q)\theta - 2n'p\omega t]$ AS MULTIPLES OF POLE PITCH OF FUNDAMENTAL FORCE WAVE

n'	Noise frequency $2n'f$	Pole pitches			
		$m' = 0$	$m' = 1$	$m' = 2$	$m' = 3$
1	100	1·0	0·15	0·07	0·05
2		0·5	0·17	0·08	0·05
3	.	0·33	0·21	.	.
4	.	0·25	0·27	.	.
5	.	↓	0·37	.	.
6			0·58		
7			1·4		
8	800		3·5		
9			0·78		
10	.		0·14		
11	.		↓		
12	.			0·29	
13				0·41	
14				0·70	
15	1 500			2·33	
16				1·75	
17				0·64	
18	.			↓	
19	.				
20	.				
21					0·47
22					0·87
23	2 300				7·0
24	.				1·16
25	.				0·54
	.				↓

56-pole 432-slot 50 c/s machine.
$q = 432$. $p = 28$. $f = 50$.
· · · Increasing.
→ Decreasing.

In normal 50 c/s machines the values of n' are usually such that the noise frequency lies between 500 and 1 500 c/s, which is of course in the range of maximum sensitivity of the human ear.

* Slots per pole equal to, say, $4\frac{6}{7}$ are to be regarded as $5 - \frac{1}{7}$ since these two equivalent numbers correspond to two different series of harmonics and the longest pole-pitch harmonic is associated with the series corresponding to $5 - \frac{1}{7}$. Similarly $4\frac{5}{7}$ is regarded as $5 - \frac{2}{7}$, etc.

Table 2

DIMENSIONS OF SIX REPRESENTATIVE MACHINES

Machine	D_c	D_g	L_c (gross)	L_c (net)	p	q	g_{min}	g_{max}	s	t	d	$\frac{1}{2}W$
	in	in	in	in			in	in	in		in	in
A	144	132	45	$37\frac{1}{8}$	25	360	0·25	0·25	0·54	0·612	3·50	2·94
B	168	156	40	$32\frac{7}{8}$	28	432	0·30	0·30	0·54	0·593	3·50	3·13
C	183	165	47·5	40·0	21	360	0·43	0·645	0·65	0·790	5·25	4·38
D	183	165	47·5	40·0	21	360	0·50	0·50	0·65	0·790	5·25	4·38
E	47	39	8·5	$7\frac{3}{8}$	10	144	0·075	0·148	0·39	0·461	2·12	2·13
F	47	39	8·5	$7\frac{3}{8}$	10	150	0·075	0·148	0·37	0·447	2·22	2·13

This emphasizes the importance from the point of view of operating staff of reducing or eliminating this noise.

This discussion concerning the pole pitch of the noise harmonic is illustrated in Table 1, which relates to machine B of Table 2. The pole pitches are given relative to those of the fundamentals of the force waves. From the Table it is clear that the wave of longest pole pitch (7·0) occurs with $n' = 23$ and $m' = 3$. Other force waves likely to produce noise because of their long pole pitches are those associated with $n' = 15$, $m' = 2$ and $n' = 7$, $m' = 1$. Experience shows that noise is almost invariably associated with $m' = 1$; since 8 is the h.c.f. of 56 and 432, the longest possible pole pitch in this machine ($7 = 56/8$) corresponds to $n' = 23$ and $m' = 3$.

(3.2) Amplitude of Harmonic Waves

(3.2.1) Slot Permeance Harmonics.

In a given design of machine the amplitude of the slot permeance wave (Fig. 2) is controlled largely by whether the slots are open or semi-closed. The open slot, particularly with a short air-gap, produces substantial permeance harmonics, whilst the semi-closed will produce harmonics of small amplitude. Thus, in general, noise is much less likely with semi-closed than with open-slot machines.

(3.2.2) M.M.F. Harmonics.

The amplitudes of the m.m.f. harmonics are a function of the geometry of the pole shoe and associated air-gap. The length of the air-gap and the ratio of pole arc to pole pitch are controlled by other, overriding factors, such as permissible polar leakage flux and short-circuit ratio, so that little modification is possible in order to reduce harmonics. However, in many cases it is possible to reduce harmonics by tapering the air-gap between the pole shoe and the stator bore, as shown by the dotted lines in Fig. 1. The ideal taper can be obtained by harmonic analysis of a number of degrees of taper, but even here the amount may be limited by other electrical and mechanical considerations.

(3.2.3) Force Harmonics.

The influence of the amplitude of the m.m.f. and permeance harmonics on the amplitude of the force harmonics is best illustrated by a simple example. Assume the m.m.f. wave of eqn. (3) is represented by $a \cos p\theta + b \cos 3p\theta$ and the permeance wave by $c \cos q\theta$, so that K_n is represented by a and b, and K_m by c, the values of a, b and c being obtained by the methods described in the two previous paragraphs.

Eqn. (3) can then be written

$$B = (a \cos p\theta + b \cos 3p\theta)(c \cos q\theta)$$

For reasons given later, only product terms with negative signs in the coefficient of θ are considered, so that the expression becomes

$$\frac{ac}{2} \cos (p - q)\theta + \frac{bc}{2} \cos (3p - q)\theta$$

Then, in eqns. (4) and (5), the values of K_{nm} are given by $\frac{1}{2}$ and $\frac{1}{2}bc$. In order to obtain the force waves of eqn. (6) th expression is squared:

$$\tfrac{1}{2}\left(\frac{ac}{2}\right)^2 \cos 2(p - q)\theta + \tfrac{1}{2}\left(\frac{bc}{2}\right)^2 \cos 2(3p - q)\theta$$
$$+ \frac{ac}{2}\frac{bc}{2} \cos (4p - 2q$$

The coefficients $\frac{1}{2}(\frac{1}{2}ac)^2$, $\frac{1}{2}(\frac{1}{2}bc)^2$ and $\frac{1}{2}ac\frac{1}{2}bc$ thus correspond the various values of $K'_{n'm'}$ in eqn. (6). In this simple examp the amplitudes of the force harmonics can be obtained inspection of a, b and c; in an actual machine where this wou be difficult, since there would be a number of values of bo K_n and K_m, the method to be adopted is explained in Section

(4) INFLUENCE OF MACHINE DIMENSIONS ON NOIS

It is obvious that the noise emitted by a machine is affect by such factors as the stiffness of the core and the gap diamete The influence of such factors is considered in further det below.

(4.1) Moment of Inertia and Deflection of Core

In machines with a small number of poles a high cross-sectio inertia of the core is usually obtained owing to the inherent deep cores of these machines. With a large number of pol the core is inherently relatively shallow and the inertia tends be low. Thus excessive deflection of the stator core by harmon force waves, leading to noise, is in this respect less likely machines with a small number of poles, e.g. $2p < 10$.

As is shown later, the deflection of the core which produc noise is of the order of 10^{-6} in, whilst the manufacturi tolerances between the outside diameter of the core and t bore of the frame may be of the order of thousandths of inch. It thus follows that, relative to the low-amplitude defle tion causing noise, the mechanical coupling between the co and frame will usually be small. The situation is furth complicated in smaller machines by the use of ring punching which are more likely to produce some mechanical coupli between core and frame than are the segmental punchings us in the larger machines. For these reasons the inertia of t frame has not been included in subsequent calculations and t core has been considered as a homogeneous steel ring.[12] In t finished machine the mechanical coupling between core a frame may, owing to differential expansion following heati be sufficiently tight to play a significant role, and in such cas the measured noise will usually be lower than that predicted.

(4.2) Deflection and Natural Frequency of Core

The static radial deflection of the core is obtained from t simple theory of loaded beams. This assumes that with a wa having $2(2n'p - m'q)$ nodes the periphery of the core consists $2(2n'p - m'q)$ simply supported straight beams[7] loaded with

uniformly distributed force equivalent to the actual sinusoidal force wave.

The total deflection is obtained by multiplying the static deflection by[6]

$$\frac{1}{1-(f_h/f_0)^2}$$

As $f_h \to f_0$ resonance is approached and the noise is greatly amplified, so that this condition should be avoided by suitable modifications to the dimensions of the core.

In the calculations of f_0 the weight per unit circumferential length of the core must include the weight of the teeth.

The mechanical coupling between the winding and the slot surfaces may be taken as negligible in view of the small deflections of the core and the high frequency of the vibrations. Therefore the loading of the core by the windings is excluded from the calculation of f_0.

(4.3) Relation of Machine Diameter to Propagation of Noise

The extent to which a machine, regarded as a circular cylinder, will emit noise is dependent on the linear velocity of the force harmonic [eqn. (6)] at the periphery of the frame. The angular velocity of the force harmonic, relative to that of the rotor, is given by the ratio of the time and space coefficients of eqn. (6), i.e.

$$r_1 = \frac{2n'p}{2n'p - m'q} \quad . \quad . \quad . \quad . \quad . \quad (7)$$

Fourier analysis. Numerical calculation by hand is tedious and for this reason the authors have had a computer programme prepared which calculates the required curves and also the amplitudes of all harmonics up to any required order.

(5.2) Rotor M.M.F. Harmonics

The m.m.f. variation shown in Fig. 1 is calculated by Carter's[9] equations and the amplitude of the harmonics obtained by Fourier analysis. In order, for various reasons, to reduce the harmonics in this wave it is common practice to taper the air-gap over the pole shoe, as shown by the dotted line in Fig. 1. An allowance for this is made in the calculation by assuming that the flux density over the pole arc varies inversely as the length of the air-gap, the remainder of the curve being calculated from Carter's equations. Here again a computer programme has been prepared to give the required results.

The rotor pole-face usually carries a squirrel-cage winding fitted in semi-closed slots. In practice, the width of the slot opening, expressed as a fraction of the length of the air-gap, is so small that its effect on the force waves is negligible.

(6) NUMERICAL CALCULATION OF NOISE

A synchronous machine exhibiting the type of noise investigated in the paper is that given as A in Tables 2 and 3.* The method of calculating the high-frequency noise of this machine is as follows.

Table 3

NOISE PARAMETERS WITH CALCULATED AND OBSERVED NOISE LEVELS OF MACHINES A–F

Machine	s/g	n'	m'	r	f_h	$v/1\,125$	k_1	f_0	k_2	Noise level		Loudness	
										Calculated	Observed	Calculated	Observed
					c/s			c/s		phons	phons	sones	sones
A	2·16	7	1	2·5	700	2·35	1·20	346	−0·36	74	Noisy	10·5	Noisy
B	1·80	23	3	3·5	2 300	11·2	1·00	161	−0·005	32	Quiet	0·59	Quiet
C	1·29	17	2	3·5	1 700	12·0	1·00	112	−0·006	33	<59·0	0·63	3·8
D	1·29	17	2	3·5	1 700	12·0	1·00	112	−0·006	9	—	0·12	—
E	3·93	7	1	2·5	700	1·92	1·42	394	−0·51	70	83	8·0	20·0
F	3·72	7	1	1	700	0·76	3·50	2 470	1·21	46 } 56	70 } 71	1·50 } 3·1	8·0 } 9·0
		8	1	1	800	0·87	5·00	2 470	1·24	55	66	2·90	6·1

Line frequency, 50 c/s for all machines.

Therefore the velocity of the force harmonic at the core periphery is

$$v = \frac{0\cdot 52 f n' D_c}{(2n'p - m'q)} \quad . \quad . \quad . \quad . \quad . \quad . \quad (8)$$

The level of radiated noise is very sharply dependent on whether v is above or below the velocity of sound in air.[7] As v falls below the velocity of sound there is a steep reduction in the intensity of the radiated noise, but when v is at and above the velocity of sound the noise radiated is more or less independent of v. From eqns. (7) and (8) it can be seen that a long-pole-pitch force harmonic, i.e. $(2n'p - m'q)$ small, is more likely to cause noise in a large-diameter machine than in a small one, so that in general the possibility of undue noise increases with the diameter of the machine.

(5) CALCULATION OF AMPLITUDE OF HARMONICS

(5.1) Stator-Slot Permeance Harmonics

The permeance variation shown in Fig. 2 can be calculated from the equations given by Coe and Taylor,[8] and the amplitudes of the harmonics in the resulting curve are obtained by

The h.c.f. of q and $2p$ is 10; it is the least value of $|2n'p - m'q|$ and is obtained when $n' = 7$ and $m' = 1$.

The harmonic in the force wave most likely to cause noise is thus

$$\cos\left[(2 \times 7 \times 25 - 1 \times 360)\theta - 2 \times 7 \times 25\omega t\right]$$

or $\cos(10\theta + 350\omega t)$ [see eqn. (6)].

This force wave thus has 10 space periods and a frequency of $(350 \times 50)/25 = 700$ c/s.

The peripheral velocity of this harmonic at the outer diameter of the core is

$$\frac{50 \times 0\cdot 520 \times 7 \times 144}{(2 \times 7 \times 25 - 360)} = 2\,640\ \text{ft/s [see eqn. (8)]}$$

The noise reduction factor[7] for this velocity is 1·2.

The amplitudes of the permeance harmonics corresponding to the noise-producing wave $\cos(10\theta + 350\omega t)$ are

$$\mathscr{P} = a_0 + a_1 \cos 360\theta + a_2 \cos 720\theta + \ldots \text{ [see eqn. (2)]}$$

* Machines A and B are those also designated A and B in Carter's paper.[1]

where
$$a_0 = 0{\cdot}680$$
$$a_1 = 0{\cdot}646$$
$$a_2 = -0{\cdot}33, \text{ etc.}$$

The coefficient of $\cos 360\theta$ in $\mathscr{P}^2$ is

$$\begin{aligned} &2a_0a_1 + a_1a_2 + a_2a_3 + \ldots \\ &= 0{\cdot}880 - 0{\cdot}213 \\ &= 0{\cdot}675 \end{aligned}$$

Similarly the corresponding m.m.f. wave is

$$M = b_1 \cos 25(\theta - \omega t) + b_3 \cos 75(\theta - \omega t) + \ldots \text{ [see eqn. (1)]}$$

where
$$\begin{array}{lll} b_1 = 1{\cdot}188 & b_9 = -0{\cdot}070 & b_{17} = 0{\cdot}016 \\ b_3 = -0{\cdot}197 & b_{11} = 0{\cdot}0184 & b_{19} = 0{\cdot}0004 \\ b_5 = -0{\cdot}040 & b_{13} = 0{\cdot}0196 & b_{21} = -0{\cdot}009 \\ b_7 = 0{\cdot}099 & b_{15} = -0{\cdot}0283 & \end{array}$$

The coefficient of $\cos 350(\theta - \omega t)$ in M^2 is $0{\cdot}5b_7^2 + b^5b_9 + b_3b_{11} + b_1b_{13} + b_1b_{15} + b_3b_{11} + b_5b_{19} = 0{\cdot}0099$.

The above expressions for permeance and m.m.f. are in per-unit values and the factor required to obtain flux density is calculated from the electrical design in the usual way; in this case it is 66 200 maxwells/in^2. Therefore, the coefficient of $\cos(10\theta + 350\omega t)$ in the expression for B^2 is

$$\begin{aligned} &0{\cdot}5 \times 0{\cdot}675 \times 0{\cdot}0099 \times (66\,200)^2 \\ &= 14{\cdot}6 \times 10^6 \end{aligned}$$

The amplitude of the force harmonic is

$$\begin{aligned} &1{\cdot}39 \times 14{\cdot}6 \times 10^6 \times 10^{-8} \\ &= 0{\cdot}203 \text{ lb/in}^2 \end{aligned}$$

The deflection, δ, of a circular ring due to a 20-node sinusoidal force wave is given by[7]

$$\delta = \frac{F_l D_c^3}{16E \times 10^3 \times I}$$

where
$$F_l = \frac{2}{\pi} \times 0{\cdot}203 \times \frac{\pi D_g}{20} = 2{\cdot}7$$

$$\begin{aligned} I &= \left[\left(\frac{D_c - D_g}{2}\right) - d\right]^3 \times \frac{1}{12} \\ &= 2{\cdot}5^3/12 \\ &= 1{\cdot}30 \end{aligned}$$

Therefore $\delta = 12{\cdot}8 \times 10^{-6}$ in

This is the static deflection due to the magnetic force on the core and must be modified to allow for the dynamic conditions which actually obtain. The appropriate modifying factor is given by

$$\frac{1{\cdot}11\delta}{1 - (f_h/f_0)^2}$$

In this case $f_h = 700$ c/s, $f_0 = 346$ c/s and $1{\cdot}11$ is a factor to allow for punching insulation. Therefore the dynamic deflection is given by

$$\begin{aligned} \frac{1{\cdot}11\delta}{1 - (700/346)^2} &= 0{\cdot}36 \times 12{\cdot}8 \times 10^{-6} \text{ in} \\ &= 4{\cdot}63 \times 10^{-6} \text{ in} \end{aligned}$$

Various laws have been proposed for determining the variation of noise with the distance of the observer from the source and the mode of vibration.[7] In view of the complexity of the problem of deciding whether the machine resembles a sphere or a cylinder it has been decided to use the simple inverse-square law.

Therefore the reduction factor here for an observer standin 6 ft from the core is $(\frac{1}{2})^2$.

The sound intensity* is then given by[7]

$$\begin{aligned} 10 \log_{10} [1{\cdot}3 \times 10^{-4} \times (2 \times 4{\cdot}63 \times 10^{-6})^2 \times 10^{16} \times 700^2 \\ \times \tfrac{1}{4} \times 1{\cdot}2 \end{aligned}$$

$$= 72{\cdot}0 \text{ dB } (74{\cdot}0 \text{ phons})$$

It can easily be shown that 74 phons corresponds t 10·5 sones.[10]

(6.1) Discussion of Calculated and Observed Noise Levels

(6.1.1) General.

The calculations carried out on machine A were repeated o five further machines whose dimensions are given in Table 2.

Table 3 gives the parameters which are significant in th generation of noise; it also includes the calculated and observe noise levels.

Machines A and B were put into service more than thirt years ago and meter readings of the noise are not availabl however, one of the authors was present when the machin were tested and can confirm that machine A, when excite emitted a penetrating note which could be clearly heard at considerable distance, whilst with machine B it was impossib to detect when the field switch was opened or closed, even whe standing quite close to the machine.

Machine C was recently completed and here again it wa impossible to detect closing or opening of the field circuit. Th result is confirmed by the meter readings.

Machine E was noisy; however on machine F, a duplica machine for the same duty, the number of stator slots wa changed in order to reduce the noise.

Machine D is merely a record of the calculated noise o Machine C, but assuming the air-gap to be parallel instead c shaped; this example shows that, contrary to accepted belie shaping of the air-gap does not necessarily lead to a reductio in noise, although in many cases it would do so.

(6.1.2) Influence of Ratio s/g.

Although s/g for A is only slightly greater than that for B the noise of A was very much greater than that of B; similarl s/g for E is only slightly greater than that of F but the soun level of E is much greater than that of F.

Again, although the s/g of F is nearly double that of A, I is much quieter than A. Therefore the value of s/g is not neces sarily a decisive factor in the production of noise.

(6.1.3) Influence of n and m.

The accepted view that high-frequency noise is associated wit the fundamental ($m = 1$) of the stator permeance wave and wit low-order rotor m.m.f. harmonics is confirmed by the nois machines A and E. On the other hand F, which accords wit this view, was quiet.

Thus the values of n and m in themselves are not reliabl guides in assessing whether noise is likely.

(6.1.4) Influence of Length of Pole Pitch of Harmonic Wave.

Here again a long-pole-pitch wave does not necessaril produce noise, since although A and E have relatively long pol pitches, as shown by r, the pole pitches of B and C are longer and of F much shorter.

(6.1.5) Influence of Frequency and Peripheral Velocity of Harmonic.

The values of f_h and $v/1\,125$ are quite inconclusive since hig frequency and high peripheral velocity are associated with bot noisy and quiet machines.

* 10^{-16} watt/cm^2 is the sound-intensity reference level.

(6.1.6) Influence of Stator Core Inertia.

As can be seen from the tabulated values of k_2, in each case a normally designed stator core gave a natural frequency of vibration well removed from resonance with the noise harmonic.

(6.2) Comparison of Calculated and Observed Noise Levels

In general it can be seen that the calculated noise levels are appreciably lower than those measured. It is believed that this discrepancy can be attributed to the fact that the calculations give a noise level associated with one or two frequencies only, whereas, depending on the type of meter, measurement gives either the total noise level or the noise level associated with the energy over, for example, half an octave. In the measurement of the noise on medium- and large-size machines which cannot be accommodated in a soundproof room, the effects of background noise, reflection from walls, etc., cannot be completely eliminated.

In view of these factors the agreement between the calculated and observed results is good; in particular the reduction in the noise level of E obtained by changing the number of stator slots to those used in F is 14 phons by calculation and 12 phons by measurement.

The phon readings are transformed into sones since these are units of loudness and permit a direct comparison of loudness. Thus E is 2·6 times noisier than F by calculation and 2·22 times by measurement.

(6.3) Measurement of Core Deflection, Number of Nodes of Core Vibration and Frequency

On machine E characteristics of the vibration of the outer surface of the core were investigated by a velocity-type vibrometer, and it was found that the minimum measured value of the peak dynamic deflection was about 6×10^{-6} in with an average of about 20×10^{-6} in. The corresponding calculated value is about 5×10^{-6} in. Since the measured deflection is necessarily the sum of those caused by all harmonic force waves including the fundamental, the agreement between measurement and calculation is reasonable.

The measured pattern of vibration round the outer periphery of the core of machine E showed a marked 8-node distribution producing 700 c/s noise. Both these results are in agreement with the values of n' and m', i.e.

$$|2n'p - m'q| = |2 \times 7 \times 10 - 1 \times 144| = 4$$

corresponding to a $\cos 4\theta$ wave with a frequency of $2 \times 7 \times 50 = 700$ c/s.

(7) CONCLUSIONS

The analysis shows that the high-frequency noise is caused by a combination of the following six factors, and it must be emphasized that *the likelihood of noise cannot be assessed by a consideration of any one factor or group of factors, but only by carrying out an analysis such as that given in the paper.* Difficulties experienced in the past in avoiding high-frequency noise have been largely due to a lack of appreciation of the fact that only rarely is this noise attributable to a single cause and that in most cases it is necessary to take all six factors into account.

(7.1) Amplitude of Stator Permeance Harmonics

The amplitude of the stator permeance variation (see Fig. 2) is the most important single factor likely to lead to noise, since obviously if the width of the stator-slot opening is negligibly small compared with the length of air-gap, the permeance variation is also negligible and there can be no noise of the type discussed in the paper.

The amplitude of the permeance variation rises sharply with the ratio of slot opening to air-gap, so that if other considerations permit this ratio should be as small as possible.

(7.2) Amplitude of Rotor M.M.F. Harmonics

The amplitude of the noise harmonic is also dependent on the amplitude of certain relatively high-order harmonics in the rotor m.m.f. wave (see Fig. 1). Since the general configuration of the rotor poles is determined by other design considerations, the usual method of reducing these harmonics is that of shaping the gap. However, it can be seen from Table 3, machines C and D that this shaping does not necessarily reduce the undesirable harmonics, although of course it will reduce low-order harmonics.

(7.3) Pole Pitch of Harmonics

An examination of eqn. (6) shows that, if for particular values of n' and m', $2n'p$ and $m'q$ differ by a small integer, the noise harmonic will have a long pole pitch and may therefore produce undue noise. This condition is easily recognized by an examination of the number of slots per pole. If this number is, for example, $7\frac{1}{5}$ (see Table 2, machine A), the numerator of the fractional portion of this number is 1, which corresponds to a difference between $2n'p$ and $m'q$ of 1, and thus a wave with maximum length of pole pitch is likely to lead to noise. Conversely, with $8\frac{4}{7}$ slots per pole (see Table 2, machine D), the numerator here is 3, giving a relatively short pole pitch for $m' = 1$ with much less likelihood of noise.

(7.4) Frequency of Noise

The empirical rule referred to in Section 3.1 states that the frequency of the noise is equal to twice the product of the line frequency and the nearest integer to the number of slots per pole. This rule has been proved analytically and has been shown to be valid for all the noisy machines in the authors' experience. However, this rule assumes that the noise harmonic is always associated with the term denoted by $m' = 1$ in eqn. (6). Now the minimum of $2n'p - m'q$ corresponding to the noise harmonic of the longest pole pitch may be associated with a value of m' other than unity. That noise associated with values of m' greater than unity has not been observed in the past is undoubtedly owing to the low amplitudes of the appropriate harmonics in the stator permeance and rotor m.m.f. waves. There is thus no certainty that the above empirical rule for noise frequency will hold in all cases.

(7.5) Peripheral Velocity of Noise Harmonic

If the linear velocity of the noise harmonic at the periphery of the stator core is appreciably less than the velocity of sound in air, then the air vibration is largely self-damping. It thus follows that small-diameter machines are less liable to be noisy than those of large diameter.

In this connection it may be noted from eqn. (6) that the angular velocity of a noise wave increases as $2n'p - m'q$ decreases, i.e. as the length of the pole pitch of the noise wave increases. Thus waves of long pole pitches inherently have a high angular velocity.

(7.6) Moment of Inertia and Natural Frequency of Vibration of Core

The stator frame may normally be assumed to play no significant part in the prediction of high-frequency noise. If for any reason, such as differential expansion effects, the stator frame does contribute to the stiffness of the core, the net effect will usually be to reduce the noise.

If calculations show that the natural frequency of the core for the mode in question is close to the noise frequency, it may be necessary to redesign the core in order to change its natural frequency.

(7.7) Prediction of Total Noise

The paper gives a mathematical analysis which makes it possible to predict, with reasonable accuracy, the open-circuit magnetic noise level of synchronous machines.

For certain special applications it may be necessary to restrict the noise level to a low value, e.g. 60 phons. In these cases the noise spectrum over a range of frequencies would be required in order to determine the total noise. The method developed enables this to be done, although it would probably be necessary, in view of the labour involved, to prepare a digital-computer programme for the whole calculation rather than for a part as has been done here.

It is obvious that this method of calculation is suitable for determining the magnitude of low-frequency noise, e.g. 100 c/s. The method may also be applied to normal d.c. machines.

The practicability of skewing as a means of reducing noise is discussed in Section 10.

(8) ACKNOWLEDGMENTS

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(10) APPENDICES

(10.1) To show that, if $m' = 1$, the smallest numerical value c $2\,n'p - m'q$ is obtained when n' is the nearest integer to $q/2p$

$2n'p - q$ is least when $n' - q/2p$ is least, i.e. when n' is a near as possible to $q/2p$. Since n' is an integer, n' is the intege nearest to $q/2p$.

If $q/2p = \text{integer} + \frac{1}{2}$, n' has two possible values. Th implies that there are two separate force harmonics which bot have the maximum pole pitch but are of different frequenc (see footnote in Section 3.1).

(10.2) Reduction of Noise by Skewing

Skewing is an accepted method of reducing certain undesirab effects, e.g. ripples in the open-circuit e.m.f. wave, caused by th presence of stator slot openings.

In the problem of reducing or eliminating noise it can easi be shown that a skew of one stator slot pitch rotates the forc harmonic associated with $m' = 1$ through 360° (mechanica from one end of the core to the other; similarly a skew of pitches rotates it through 720°, and so on. From Timoshenko equations[11] it thus follows that, as a direct function of the numb of nodes, this displacement of the wave by skewing very quick reduces the vibration of the core, and therefore the noise, to very small value. Thus skewing from the theoretical point c view can be regarded as a certain method of reducing or elim nating the most objectionable noise harmonic but not necessari all of them. Skewing, however, has been little used in practic for this purpose largely because of the difficulties involved i manufacture. In those cases where it has been used it appea that skewing has been successful in reducing noise in relativel small short-core machines ($L_c < 12$ in), but that occasionally i larger machines with longer cores the results have been inex plicably disappointing. These latter results are probably du to the marked departure of the long core from the assumptio explicit in this paper and implicit in Timoshenko's equations, that the core behaves as a homogeneous elastic ring. In a sma short-core machine the pressure produced by the clampin structure is undoubtedly transmitted uniformly throughout th whole core. In larger machines with long cores, certain methoc of core assembly and clamping, coupled with the use of ho rolled magnetic steel, do not ensure that the pressure exerte by the clamping fingers and rings is transmitted uniform throughout the axial length of the core.

Thus the pressure may diminish from each end of the co with actual slackness, i.e. lack of firm contact between th punchings, around the middle portion of the core. This slack ness makes the assumption mentioned above unjustified, an invalidates the analysis predicting a reduction in noise.

Thus skewing cannot be regarded as a satisfactory or certai method of avoiding noise in all cases and the best solution is t eliminate the cause of noise at its source.

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SPEED-CHANGING INDUCTION MOTORS

Further Developments in Pole-Amplitude Modulation

By Prof. G. H. RAWCLIFFE, M.A., D.Sc., Member, and W. FONG, Ph.D., Graduate.

(*The paper was first received* 23*rd March, and in revised form* 3*rd June*, 1960.)

SUMMARY

An earlier paper has described the basic steps which led to a practical form of 8/10-pole induction motor, of good performance, having only one stator winding. The principle on which this speed-changing motor was based has been given the name of 'pole-amplitude modulation'. Since the publication of the original account of it, the principle has been further developed, and a number of improvements in the method have been devised and tested in practice on several new forms of 8/10-pole motor. This paper discusses the theory and tests on these new machines, which are of interest both in themselves and for the further light which they throw on the principles of pole-amplitude modulation. The tests were completely successful, and this type of machine is now in the repertory of several manufacturers as an established industrial product.

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(1) INTRODUCTION

The principle of induction-motor speed changing by pole-amplitude modulation was originally described in an earlier paper,[1] the modulation of the individual phases being carried out in the experimental machine by reversal of the second half of each phase with respect to the first half, preferably with omission of one coil group from each half-phase. The effect of chording the windings was also examined, and windings of two-thirds full pitch for the initial 8-pole winding were found to be the most advantageous. Analyses of the resultant m.m.f. waveforms for these various windings and connections were given in the earlier paper, which described theory and tests leading to the development of an economic and efficient 8/10-pole speed-changing squirrel-cage induction motor.

The decision to omit certain coils from the winding, on modulation, was reached semi-intuitively in the early stages of the work; and the favourable effect of chording, though foreseen, was rather more striking in practice than had been originally expected. The theory behind the first successful methods, already described, has now been more fully developed; and, as so often happens, this theory has enabled certain further improvements and new designs to be obtained. Particulars are given of the theory for several new types of 8/10-pole speed-changing induction motor; the same principles being equally applicable to certain other pole combinations, as discussed in the original paper. The possible pole combinations to which these methods can be directly applied are those in which neither pole number is a multiple of three, or those which are a multiple repetition of such a pole combination. For example, 8/10 is directly possible but 6/8 is not; and 24/30 is directly possible but 18/24 is not.

In addition to discussing the full theory of the latest developments, the paper gives test results both for two small machines and for four much larger machines made by two industrial companies to the authors' designs, and operating on the same principles as the small machines previously described. After the publication of the original paper[1] about this method of speed change, many misgivings were expressed about the likelihood of parasitic torques, and of noise. For this reason, exceptionally thorough tests were made in relation to these aspects of performance.

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
Professor Rawcliffe is Professor of Electrical Engineering, University of Bristol.
Dr. Fong is in the Dept. of Electrical Engineering, University of Bristol.

(2) HARMONIC EFFECTS IN THE PROCESS OF MODULATION

(2.1) Modulation by a Harmonic Series

When amplitude modulation of a single phase-winding is effected by reversing one half of the winding with respect to the other half, it is equivalent to modulation of the original m.m.f. wave by one cycle of a rectangular wave. A variant of this is to omit two sections of the winding (for example, the fourth and eighth coil groups in an 8-pole phase-winding), one half of the remaining coil groups being reversed with respect to the other half. This is equivalent to modulation of the original m.m.f. wave by one cycle of a shortened rectangular wave, the length of each rectangle being less than a full half-wavelength; in this example, three-quarters of a half-wavelength. Such modulation is fully explained in Section 2 and Fig. 1 of the original paper.[1]

Modulation by a rectangular wave is equivalent to multiplication of the original wave by a harmonic series of the form

$$y = \frac{4}{\pi}h\left(\cos\alpha\sin\theta + \frac{1}{3}\cos 3\alpha\sin 3\theta + \frac{1}{5}\cos 5\alpha\sin 5\theta \ldots + \frac{1}{n}\cos n\alpha\sin n\theta\right)$$

where 2α is the angle by which the length of one rectangular half-wave falls short of π electrical radians.

Each term in this harmonic series will produce its own modulation resultants. To take a particular case, the resultant pole numbers when an 8-pole wave is modulated will be as shown in Table 1. The pole numbers resulting from modulation are shown in the right-hand column, their amplitudes, of course, diminishing steadily as the order of modulating harmonic increases. Those resultant pole numbers which are a multiple of 6 (i.e. all triplen harmonics) will disappear when the three phase-windings are combined, and may thus be ignored. The word 'zero' indicates these in Table 1. Those which are marked with a positive sign set up positively rotating fields, and those marked with a negative sign set up fields rotating in the opposite direction. (The terms 'positive' and 'negative' here are, of course, purely relative). Besides the 10-pole field, which in this case is the desired resultant, the other main positively rotating fields are the 22-pole and 34-pole fields, the magnitude of the latter being small, and the former being further reduced by chording. The important negatively rotating component is the 2-pole field, the amplitude of which is normally much larger than that of any other, though the 14-pole field is not always negligible.

Table 1

Example of Pole Numbers Resulting from Pole-Amplitude Modulation by a Harmonic Series

Original wave	Modulation by	Resultant pole numbers
Fundamental 8-pole	2 poles (fundamental)	zero + 6 and 10
	6 poles (3rd harmonic)	− − 2 and 14
	10 poles (5th harmonic)	− zero 2 and 18
	14 poles (7th harmonic)	zero + 6 and 22
	18 poles (9th harmonic)	+ − 10 and 26
	22 poles (11th harmonic)	− zero 14 and 30
	26 poles (13th harmonic)	zero + 18 and 34

In order to reduce the 2-pole field to a small value, the modulating wave, described by the harmonic series, must be so chosen that the harmonics which produce the 2-pole field are reduced in magnitude. In this particular case, the third and fifth harmonics in the modulating wave lead to a resultant 2-pole field. The omission of the fourth and eighth coil groups is equivalent to reduction of the length of the modulating rectangle to $3\pi/4$; and the factor of reduction of the mth harmonic thus is $\cos m\pi/8$, as shown by the Fourier series given above. Where $m = 3$ this factor is $+0{\cdot}383$, and where $m = 5$ it is $-0{\cdot}383$. An investigation of this particular case has thus shown that the 2-pole content is very much reduced where shortened rectangular modulation, rather than modulation by a full rectangular wave, is used.

(2.2) Shaped Modulating Waves

Omission of coils has hitherto been considered only in terms of whole coil groups, but it had thus become clear, from theory and experiment combined, that graded omission of coils would give a modulating effect which more nearly approached the ideal of modulation by a pure sine wave. Accordingly it was decided to use modulation by a stepped wave, which approaches more closely to a sine wave than does any single rectangle, of whatever pitch. The modulating wave is defined in Fig. 1(*b*), and its modulating effect on the original 8-pole wave, here shown conventionally by a series of rectangles, is illustrated in Fig. 1(*c*). This Figure as a whole should be compared with Fig. 1 in the earlier paper,[1] which first described the method of pole-amplitude modulation.

The practical method of obtaining this form of modulation in a 48-slot machine is to omit one coil out of two in the first, fourth, fifth and eighth coil groups, instead of both coils of the fourth and eighth coil groups only; choosing for such omission the first of the two coils in the first and fifth coil groups, and the second of the two coils in the fourth and eighth coil groups, these being the coils nearest to the zero points of the modulating wave.

The two rectangular modulating waves used in the original experiments on this method are shown in Figs. 2(*a*) and (*b*), together with the new stepped modulating wave in Fig. 2(*c*). It

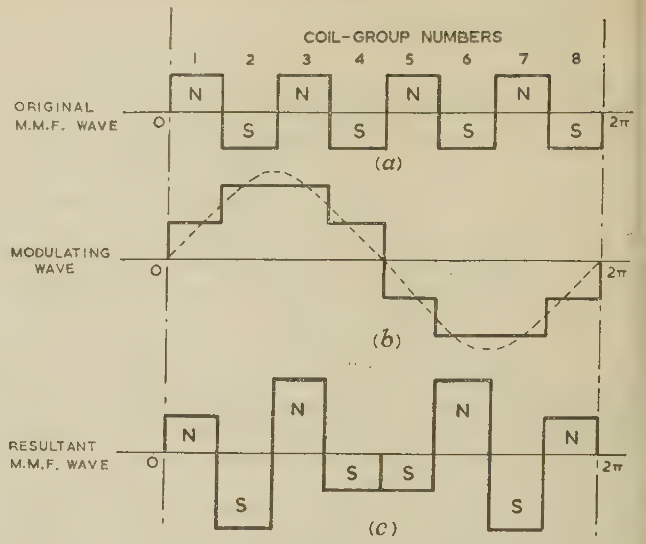

Fig. 1.—Pole-amplitude modulation by a nearly sinusoidal wave

8 poles, modulated to 6/10 poles.

To modulate, reverse groups 5, 6, 7 and 8 with respect to groups 1, 2, 3 and 4, omitting half the coils in groups 1, 4, 5 and 8. Omit the coils nearest to the zero points of the modulating wave.

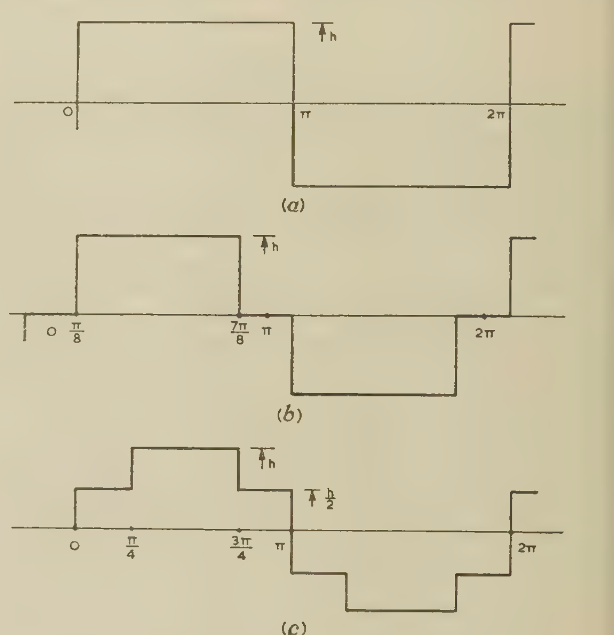

Fig. 2.—Development towards sinusoidal modulation.

(*a*) Full rectangular modulation.
(*b*) Shortened rectangular modulation.
(*c*) Stepped rectangular modulation.

can be shown that the amplitude of the mth harmonic of the modulating wave is given, respectively, in the three cases, by

$$\frac{4h}{\pi m}(1);\ \frac{4h}{\pi m}\left(\cos\frac{m\pi}{8}\right);\ \text{and}\ \frac{4h}{\pi m}\,\frac{1+\cos\frac{m\pi}{4}}{2} = \frac{4h}{\pi m}\cos^2\frac{m\pi}{8}$$

where $m = 1, 3, 5$, etc.

The ratio between the mth harmonics is thus

$$1 : \cos\frac{m\pi}{8} : \cos^2\frac{m\pi}{8}$$

and for $m = 3$ and $m = 5$, which are the important cases i

ractice since these are the harmonics of greatest amplitude, the atios are, respectively, 1 : 0·383 : 0·147. The resultants of he undesired modulation by the harmonics in the modulating wave thus become smallest, compared with the products of the desired modulation by the fundamental, when the modulating wave (c) is employed. Since it is virtually as easy to modulate n this latter way as by using the modulating wave (b), this new alternative will almost inevitably be preferred.

The resultant harmonic content of the final 3-phase modulated m.m.f. wave is shown in Table 2, column (c). A winding of two-thirds full pitch for 8 poles was chosen, for reasons which will be explained in Section 2.5. The results of the earlier forms of modulation were discussed in the previous paper,[1] in which the development of the method was described; and the harmonic contents of such an 8-pole machine, modulated by the waves (a) and (b) of Fig. 2, are also recorded in Table 2,

Table 2

M.M.F. Harmonics of 8/10-Pole Motors in the Modulated (10-Pole) Connection

Pole number	(a) Modulation as Fig. 2(a)	(b) Modulation as Fig. 2(b)	(c) Modulation as Fig. 2(c)
	%	%	%
2-pole	58·0	24·5	7·5
10-pole	100·0	100·0	100·0
14-pole	28·0	11·6	9·2
22-pole	10·0	9·4	5·4
26-pole	7·5	7·2	9·3

All windings are two-thirds full pitch.

columns (a) and (b), for comparison. It is thus clear that the principle of shaping the modulating wave, so that it approaches a sine wave, has given yet a further improvement in performance.

This method is perfectly general, and the only slight restriction on its application is that it can only be applied strictly in this form where the number of coils per group is even. However, when the number of coils per group is odd it will usually be sufficient to omit half the coils-plus-one; and, in the case of three coils per group, successful modulation is given by omitting two coils out of three in the first, fourth, fifth and eighth coil groups. The total proportion of the winding omitted on modulation is ($\frac{2}{3} \times \frac{1}{2}$) of the whole; i.e. one-third of the whole winding instead of one-quarter. The winding factor of the remaining two-thirds is, however, improved, and the likely overall difference in performance is negligible. It is, however, also possible to omit half the coils-less-one, thus omitting only one-sixth of the whole winding, although this gives a less good waveform. The results of modulating a 72-slot machine (3 slots per pole per phase for 8 poles) in each of these two possible ways are shown in columns (b) and (e) of Table 6. Column (b) of this Table corresponds to modulation 02–3–3–20 per half phase-winding: column (e) to modulation 001–3–3–100 per half phase-winding. It will be seen that modulation as column (e) gives a low harmonic content and a value of B_8/B_{10} very near to unity, and is almost certainly to be preferred to modulation as column (b). It is clear that the ideal, of pole-amplitude modulation by a pure sinusoidal wave, is capable of being more nearly realized in practice if the coils to be omitted on modulation are judiciously chosen.

(2.3) Division of Coils for Modulation

The principle of sinusoidal modulation is extensible even to cases where there is only one coil per group; for it is only necessary to divide the coils on the first, fourth, fifth and eighth poles into two equal sections, in order to be able to leave out half these coils on modulation. It is not necessary to leave out exactly half a coil, because the two sections need not be made equal; and it may even be an advantage to be able to regulate the relative flux densities and harmonic contents for the two speeds by varying the proportion between the sections of the divided coils. Manufacturing difficulties may, of course, arise in relation to this proposal, particularly for large low-voltage machines where the number of conductors may be small; but the possibility ought certainly to be considered, where it is practicable to make use of it.

There is even the further possibility of winding all the coils of the first, fourth, fifth and eighth coil groups in two sections, in every case; and thus of carrying out stepped-wave modulation by omitting some proportion of each coil in these coil groups, rather than by omitting the whole of certain coils and including the whole of other coils. It would be possible then to choose exactly the proportion of these coil groups to be omitted: it would not need to be exactly one-half or two-thirds, for example. The modulating wave could thus be shaped very nearly to the true sinusoidal form. This possibility is recorded, but the authors have not tested it experimentally. It is likely to lead only to marginal improvements, and the complexity of having to wind many of the coils in two sections is such that this procedure would be unlikely to be adopted except upon the certainty of substantial advantage. A further degree of flexibility is nevertheless introduced by this alternative method of shaping the modulating wave.

(2.4) Rectangular Modulation of Shaped M.M.F. waves

The successful development of an 8-pole winding quasi-sinusoidally modulated to 10 poles, in which the number of coils per group in each half phase-winding, after modulation, was successively 01–2–2–10, raised the possibility that it might not be necessary to re-insert the omitted four coils, on reversion to the unmodulated 8-pole condition. On this basis the 8-pole machine would be wound, as normally, in 48 slots, but the initial coil grouping in each phase-winding would be 01–2–2–10—01–2–2–10. Change of speed would be effected simply by reversing the second half of each phase winding with respect to the first.

There is a further possible embodiment of the same idea. Since only 36 coils were actually included in this last winding for 48 slots, it could be wound in a 36-slot stator, but without leaving any spaces. The coil grouping of each phase-winding would then be 1–2–2–1—1–2–2–1. (Alternatively, of course, such a winding could be wound in 72 slots with a coil grouping of 2–4–4–2—2–4–4–2, without any change whatsoever in principle). All the coils would be in circuit at both speeds, either for 48 slots or for 36 slots; and, as before, only six control leads would be needed in both cases. Both alternatives were therefore considered analytically, as follows.

Analyses of the original 8-pole m.m.f. waveforms, before modulation, for the 36-coil machine, wound both in 48 slots and in 36 slots, were first performed, with the results given in Table 3.

The lowest harmonic pole number in a normal 3-phase 8-pole winding is, of course, 40. Therefore, whilst these special waveforms contain even and sub-harmonics which are not present in a normal 8-pole machine, they are by no means intolerable waveforms, as is shown both by these analyses and by graphical step-by-step constructions of the m.m.f.'s. Further, the 48-slot machine was tested both on no load and full load when connected for 8 poles with 12 coils omitted, and the results were reasonably satisfactory.

Compression of this winding into 36 slots would, however,

Table 3

M.M.F. Harmonics in 8-Pole Unmodulated Winding, with Coil Grouping 1–2–2–1, etc.

m	Pole numbers	Wound in 48 slots	Wound in 36 slots
		%	%
2	4-pole	24·2	20·3
4	8-pole	100·0	100·0
8	16-pole	8·6	3·2
10	20-pole	1·0	4·1
14	28-pole	5·4	2·9

All windings two-thirds full pitch.

clearly be economical of material, and is thus desirable if it can be done without detriment to the performance. The result of such compression is to produce a fractional-slot 8-pole winding of irregular coil-group distribution. A normal 3-phase 8-pole fractional-slot winding, in 36 slots, has a coil-group sequence of 1–2–1–2–1–2–1–2 both for each phase individually and, thrice repeated, for the coil groups irrespective of phase; whereas this new winding has a coil-group sequence 1-2-2-1-1-2-2-1 for each phase, and a sequence 1-1-2-2-1-1-2-2, thrice repeated, for all the coil groups taken together. A clock diagram of this new winding is shown in Fig. 3.

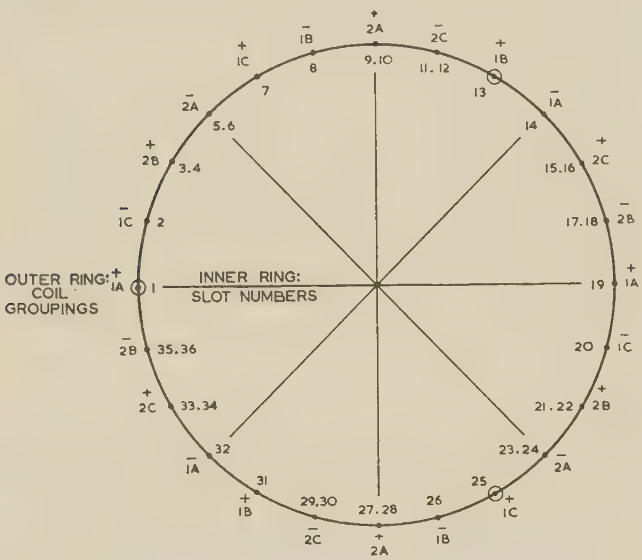

Fig. 3.—Fractional-slot winding for pole-amplitude modulation. 8/10 poles.

8 poles. 36 slots. $1\frac{1}{2}$ slots per pole per phase.
Coil pitch 3 slots = two-thirds full pitch for 8 poles.
Coil-group sequence per phase: 1–2–2–1—1–2–2–1.
Overall coil-group sequence: 1–1–2–2–1–1–2–2, etc.
Modulation by reversal of half of each phase-winding.

⊙ Ideal phase origins.

The basic modulation diagram of a winding according to these last principles is shown in Fig. 4. Since modulation is now to be effected by simple reversal of half of each phase-winding, the modulating wave is a full rectangle rather than a stepped wave; but it will be seen that the resultant wave, after modulation, is the same in this case as in that shown by Fig. 1.

The unmodulated m.m.f. waveform, as shown in Fig. 4(*a*), is however, preshaped for sinusoidal modulation, and is therefore less completely regular than the m.m.f. waveform of a standard 8-pole winding. It is fairly clear, from inspection, that there is a periodic variation in the m.m.f. arising from this unorthodox

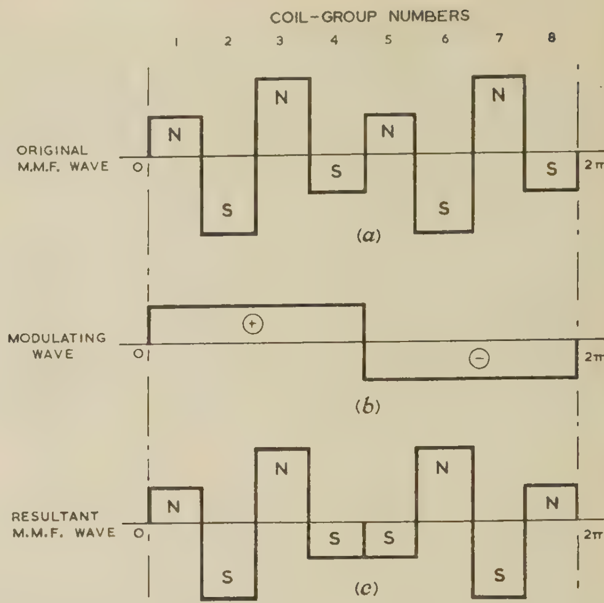

Fig. 4.—Pole-amplitude modulation of an irregular fractional-slot winding.

8 poles. Modulation to 6/10 poles.
To modulate, reverse coil groups 5, 6, 7 and 8 with respect to coil groups 1, 2, 3 and 4.
Original coil grouping: 1–2–2–1—1–2–2–1.

8-pole winding, which passes through two cycles of change whilst traversing each complete phase-winding: in other words, in addition to the 8 poles which are produced by the alternate connections of the coil groups there will be a 4-pole sub-harmonic field. The m.m.f. waveforms for such an unorthodox 8-pole winding, wound 2–4–4–2, etc., in 72 slots, are shown in Fig. 5 for the two usual vector positions. Modulation to 10 poles is achieved in the simplest possible way, by reversing the second half of each phase-winding with respect to the first. The resultant 10-pole waveforms for the same two vector positions are shown in Fig. 6. The coil pitch is 6 slots, whereas the pole pitch for 8 poles is 9 slots, and the winding is therefore wound two-thirds full pitch for 8 poles, which is five-sixths full pitch for 10 poles. The complete m.m.f. waveform analyses for the two pole numbers are given in Table 6, column (*g*).

Constructionally, this type of winding is the best of all the various types of 8/10-pole winding using the pole-amplitude modulation principle, because all the coils are completely identical and the internal connections are exceedingly simple. The winding is in no way more complicated than a standard single-speed winding, the only extra connections being the three leads from the centre of each phase-winding.

Once the principle of preshaping the m.m.f. wave in the unmodulated condition had been accepted, it became clear that there was an almost infinite variety of embodiments of this principle. Three more of them are shown in Table 6, columns (*f*), (*h*) and (*i*), and refer to three 8-pole windings wound 2–3–3–2, etc., in 60 slots, or 2–5–5–2, etc., in 84 slots, or 1–3–3–1, etc., in 48 slots. The harmonic contents of the m.m.f. waves of these windings, all chorded to two-thirds full pitch for 8 poles, are there tabulated; and it will be clear that a balance has to be struck between a low 2-pole m.m.f. in the modulated condition and a low 4-pole m.m.f. in the unmodulated condition, these two quantities tending to be mutually exclusive. In moving from column (*f*) to column (*i*) the coil-group distribution becomes progressively less uniform; and if the distribution is expressed as 1–*x*–*x*–1, in general, *x* has the successive values $1\frac{1}{2}$; 2; $2\frac{1}{2}$; 3 for these four columns.

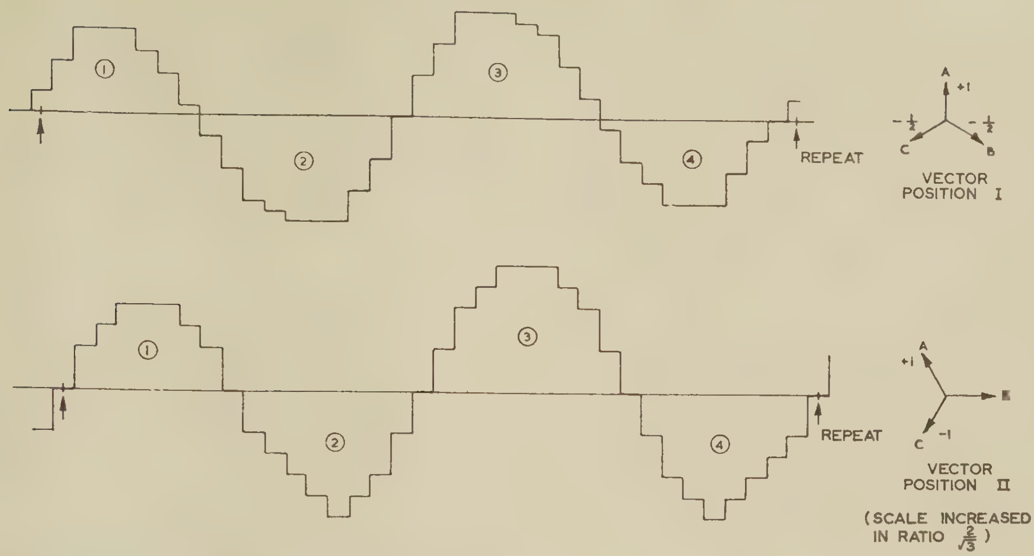

Fig. 5.—Pole-amplitude modulation of irregular fractional-slot winding. (2–4–4–2) in 72 slots.
8/10 poles.
M.M.F. waveforms before modulation. 8 poles.

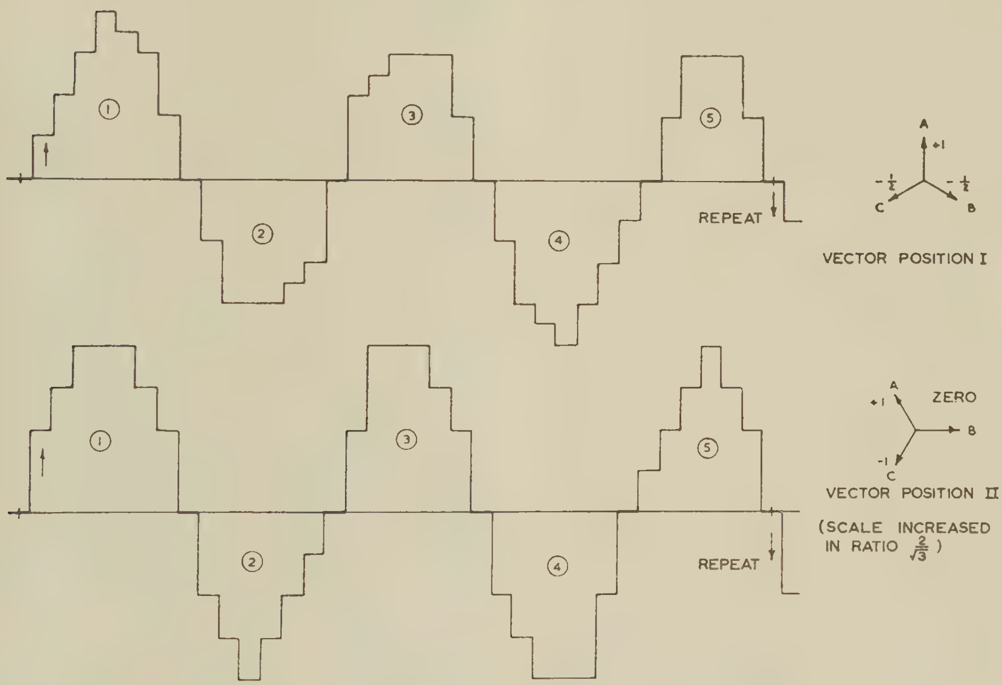

Fig. 6.—Pole-amplitude modulation of irregular fractional-slot winding: (2–4–4–2) in 72 slots.
8/10 poles.
M.M.F. waveforms after modulation to 10 poles.

It was fairly clear, from physical intuition, that substantially larger values of x were not likely to lead to satisfactory performance; but, in order to complete the analysis, the m.m.f. harmonic contents of the winding at both speeds were calculated, for values of x up to 6. The results are plotted in Fig. 7, which shows clearly that the range of values of x which lies between 2·0 and 3·0 gives, on balance, the optimum performance. Higher values of x cause progressive increase in the size of all the leading harmonics. The winding factors for both speeds necessarily also change with changing value of x, which also alters the relative air-gap flux densities at the two speeds. The winding factors K_8 and K_{10}, and the air-gap flux-density ratio B_8/B_{10}, are shown in Fig. 8, plotted against x. It is clear that from the point of view of the flux-density ratio B_8/B_{10}, $x = 3$ is to be preferred to $x = 2$, but that any further increase in x gives only marginal improvement in performance; and as explained above, this would be undesirable on other grounds.

(2.5) Effects of Chording

The undesired harmonics produced by modulation can be much reduced by an appropriate choice of chording. As will be explained in more detail in Section 3.1, the chording factor

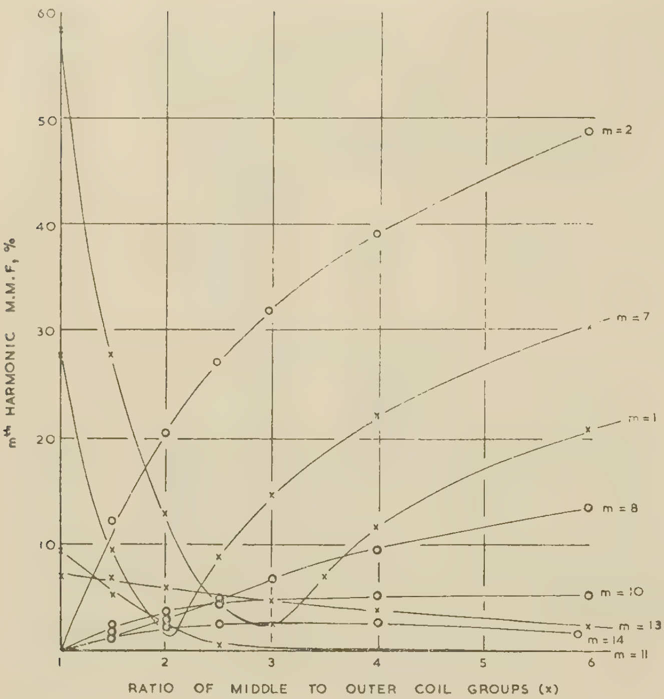

Fig. 7.—Harmonic m.m.f.'s in irregular 8/10-pole fractional-slot windings, before and after pole-amplitude modulation.

Coil grouping: 1–x–x–1—1–x–x–1.
○ M.M.F.'s before modulation, in terms of 8-pole m.m.f. ($m = 4$).
× M.M.F.'s after modulation, in terms of 10-pole m.m.f. ($m = 5$).

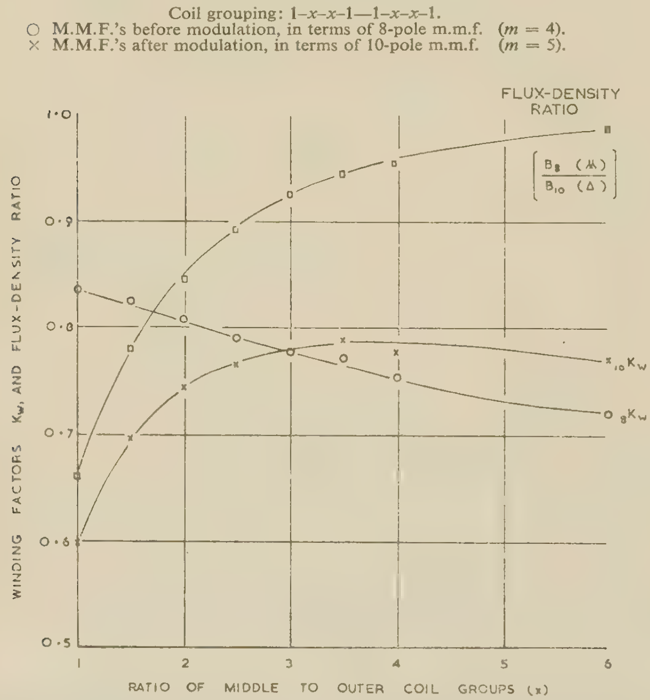

Fig. 8.—Variations of winding factors and flux-density ratio, in irregular 8/10-pole fractional-slot windings, for pole-amplitude modulation.

Coil grouping: 1–x–x–1—1–x–x–1.

of a motor with a winding of p pole pairs intended for pole-amplitude modulation is best expressed in terms of a 2-pole fundamental as $\sin m\beta/p$; where β is half the coil-pitch angle measured on the $2p$-pole scale, and m is the order of harmonic in relation to 2 poles. For a winding of two-thirds full pitch, $\beta = \pi/3$; and for an 8-pole winding of this coil pitch the chording factor in general terms is $\sin m\pi/12$. For $m = 1, 5, 7, 11, 13$, etc., which are the only significant values after modulation, the chording factors are successively as given in Table 4.

Table 4

CHORDING FACTORS OF MODULATED WINDING (10 POLES) FOR TWO-THIRDS FULL-PITCH UNMODULATED (8 POLES)

m	Pole number	Chording factor $\sin \frac{m\pi}{12}$
1	2-pole	+0·259
5	10-pole	+0·966
7	14-pole	+0·966
11	22-pole	+0·259
13	26-pole	−0·259
17	34-pole	−0·966
19	38-pole	−0·966
23	46-pole	−0·259
25	50-pole	+0·259 etc.

It will thus be observed that the chording factor operates to reduce the undesired 2-pole field and the 22-pole field, relative to the desired 10-pole field, in a ratio of about 4 : 1; and thus very greatly improves the m.m.f. waveform. This aspect of chording was very clearly shown in the original paper,[1] and especially in Fig. 9 of that paper.

It has already been explained, in Section 2.1 and Table 1, that the original m.m.f. wave is modulated not only by the sinusoidal fundamental wave but also by a series of harmonic modulating waves; but there is a second form of cross modulation which has hitherto not been considered. Each original phase-winding contains m.m.f. harmonics of all odd orders, including third harmonics, and all these harmonics will also be modulated as well as the fundamental. In this particular case, harmonic m.m.f.'s of 24 poles, 40 poles, etc., will also be modulated by the harmonic series given in Section 2.1. The most important of these terms will be the first, and the 24-pole field on modulation by a 2-pole wave will give additional 22- and 26-pole fields, the 22-pole one being especially undesirable.

There are thus two main ways in which harmonic m.m.f.'s may be produced: harmonic modulations of the fundamental m.m.f., and fundamental modulation of the harmonic m.m.f.'s. (Harmonic modulations of harmonic m.m.f.'s will, in general, give resultants of negligibly small magnitude). Which harmonic m.m.f.'s are considerable will depend on the number of poles which are modulated, and on the method which is used to do this; but it is probable that the harmonics in the modulating wave are more important generally than the harmonics in the original winding.

In whichever way the harmonic m.m.f.'s in one layer of the winding arise, the resultant of some of them will be reduced by chording when the m.m.f.'s due to each of the two layers are added together. The chording has to be chosen so that the greatest overall reduction in harmonic content is obtained; and, in particular, so that any harmonics of the same rotation as the fundamental are diminished as far as possible: otherwise crawling may occur. Other harmonics rotating in the opposite sense may cause noise or vibration.

In the 8/10-pole machine the possible crawling harmonics of positive rotation in the 10-pole connection are the 11th and 17th, these being the orders of harmonic relative to 2 poles. As stated in an earlier paper, the first machine with full-pitch windings did crawl at 5/11 × 600 r.p.m. = 273 r.p.m. In all the designs so far considered, for 8/10-pole motors, it has always proved to be best, on consideration of the chording factors, that the original winding should be chorded two-thirds

full pitch in relation to 8 poles, although it is not absolutely certain that this would always be the case.

The overall position with regard to different modulating methods may be summarized by saying that shaping the modulating wave towards the sinusoidal form greatly reduces both the sub-harmonics and the high harmonics in the resultant wave; and that suitable chording of the winding reduces the magnitude of some of the undesired harmonics, when the effects of the two layers are combined.

(3) THE WINDING FACTOR IN COMPLEX AMPLITUDE-MODULATED WINDINGS

(3.1) The Meaning of 'Layer Factor'

In a previous paper[1] it has been shown that the winding factor for the original types of pole-amplitude modulation can be expressed as the product of the usual spread and chord factors multiplied by a further quantity, there known as the connection factor. This last factor arises because the e.m.f.'s in successive coil groups are not cophasal after modulation; and, in simple cases, the connection factor can be assigned a meaning separate from the spread factor, and can be calculated by the methods indicated in the paper mentioned.

This reasoning, however, is only valid where the original winding is uniform, and where either no coils at all, or alternatively complete coil groups, are omitted on modulation. Where there is any departure from regularity of coil-group distribution in the original winding, or where parts of coil groups are omitted on modulation, the winding arrangement after modulation becomes so irregular that the spread factor and connection factor cease to have separate meanings, and can only be calculated directly as a product.

Since the lower layer of a double-layer winding—however connected or arranged—is always an exact reproduction of the upper layer, though displaced by an amount which depends on the chording, the chord factors can always be discerned as separate quantities and can be calculated in the usual way. The resultant fundamental or harmonic winding factor in a complex amplitude-modulated winding is therefore expressible as the product of the fundamental or harmonic chord factor multiplied by another complex factor, which includes the effects both of phase spread and coil interconnection, in one resultant. It is this 'layer-factor' (as it will hereafter be called), fundamental or harmonic, that is novel and will be discussed separately. The layer factor could, in fact, be considered as the spread factor of the complete winding. The term 'spread factor' has hitherto been so much associated with one phase band of a winding, however, that the authors think it better, at present, to coin a new term rather than extend the meaning of an old one.

As explained in Section 2.5, the chord factor—though deduced in the normal way—is usually best expressed in terms of the basic field, which, for single modulation, is of 2 poles. It is not desirable to express the chord factor in terms of the original number of poles, since the fields to be considered—both main and harmonic—are all related to 2 poles in integral ratios, but they are not in integral ratios to the original pole number. In Section 3 of the earlier paper,[1] for example, it was explained that the fundamental modulation product was five times 2 poles, and that the leading harmonic modulation product was eleven times 2 poles. This gave an observed crawling speed in the primitive test machine of 5/11 times the desired speed, exactly according to theoretical prediction.

(3.2) Calculation of Layer Factors

The 'layer factor' can be simply defined as the ratio between the vector sum of the e.m.f.'s, in one layer of the phase-winding, and their arithmetic sum; and, starting from this definition, the method of calculation of layer factors can best be considered by reference to an example. The fractional-slot winding illustrated in the clock diagram, in Fig. 3, of a 3-phase 8-pole stator wound in 36 slots, will thus be taken as typical.

It is desired to obtain the vector sum of the e.m.f.'s induced in all those coil sides of one phase which lie in one layer of the winding. The upper layer is chosen, though the choice is a matter of indifference, as explained in Section 3.1. This vector sum is divided by the arithmetic sum, to give the layer factor of the phase-winding. The second half of a phase-winding—in all ordinary cases—exactly reproduces the first half, the e.m.f.'s in the two halves being added or subtracted directly. It is therefore necessary to consider only half the winding.

One slot pitch in the machine corresponds to an electrical angle $2\pi/36$ with respect to 2 poles, and to an electrical angle $(2m\pi/36) = \phi$ (say) with respect to the mth harmonic of 2 poles. If $V \sin \omega t$ represents the e.m.f. induced in the conductors of one coil side by a particular harmonic flux, the e.m.f. induced in another coil side, distant λ slot pitches in the direction of flux-travel, is given by

$$\pm V \sin (\omega t - \lambda\phi)$$

The $\pm$ sign must be chosen appropriately, having regard to the winding direction of the particular coil side.

The numbering of the slots occupied by the first half of phase-winding A can be written down at once from Fig. 3, these numbers being prefixed by signs corresponding to the winding directions.

The slot numbers of the first half of phase winding A are $+1$, -5, -6, $+9$, $+10$, -14. If the e.m.f. of slot 1 is $+V \sin \omega t$, the e.m.f.'s of the remaining slots can be expressed, successively, as

$$\begin{gathered} - V \sin (\omega t - 4\phi) \\ - V \sin (\omega t - 5\phi) \\ + V \sin (\omega t - 8\phi) \\ + V \sin (\omega t - 9\phi) \\ - V \sin (\omega t - 13\phi) \end{gathered}$$

Adding these six e.m.f.'s by the usual processes of trigonometry, the resultant is found to be

$$2V \cos\left(\omega t - \frac{13\phi}{2}\right)(2 \cos 4\phi - 1) \sin \frac{5\phi}{2}$$

The arithmetic sum of the e.m.f.'s is, of course, $6V$; and ϕ equals $m\pi/18$. The term $\cos\left(\omega t - \frac{13\phi}{2}\right)$ simply indicates the phase angle of the resultant e.m.f., and is without effect on its magnitude.

The resultant layer factor is thus given by the coefficient of the above expression, divided by $6V$, and is therefore equal to

$$\frac{1}{3} \sin \frac{5m\pi}{36}\left[2 \cos\left(\frac{2m\pi}{9}\right) - 1\right]$$

The numerical values of the layer factor, for the fundamental m.m.f. and all the possible harmonic m.m.f.'s, are obtained by substituting a series of values for m in this expression. The fundamental layer factor for 8 poles, before modulation, is given by putting $m = 4$ in this expression; the fundamental layer factor for 10 poles, after modulation, is given by $m = 5$.

When the layer factor is multiplied by the corresponding chord factor, the resultant winding factor is obtained. A series of winding factors were thus computed for a variety of different 8/10-pole windings. The expressions used to calculate layer factors for the 12 types of winding covered by Table 6 are given

Table 5

General Expressions for Layer Factors for the 8/10-Pole Windings Considered in Table 6

Modulation method according to Table 6	Slot numbers	Layer factor
Col. (*a*)	48	$\frac{1}{4}\left(\cos\frac{m\pi}{48}\right)\left(2\cos\frac{m\pi}{4}-1\right)$
	72	$\frac{1}{6}\left(\frac{1}{2}+\cos\frac{m\pi}{36}\right)\left(2\cos\frac{m\pi}{4}-1\right)$
Col. (*b*)	72	$\frac{1}{6}\left[\sin\frac{7m\pi}{72}\left(2\cos\frac{m\pi}{4}-1\right)+\left(2\sin\frac{m\pi}{8}\cos\frac{m\pi}{4}\right)\right]$
Col. (*c*)	48	$\frac{1}{4}\left(\sin\frac{5m\pi}{48}\right)\left(2\cos\frac{m\pi}{4}-1\right)$
Col. (*d*)	120	$\frac{1}{5}\left[\left(\sin\frac{m\pi}{8}\right)\left(2\cos\frac{m\pi}{40}\cos\frac{m\pi}{120}+\frac{1}{2}\right)-\left(\sin\frac{7m\pi}{20}\cos\frac{m\pi}{120}\right)\right]$
Col. (*e*)	72	$\frac{1}{6}\left[\sin\frac{7m\pi}{72}\left(2\cos\frac{m\pi}{4}-1\right)-\sin\frac{m\pi}{8}\right]$
Col. (*f*)	60	$\frac{1}{5}\left[\left(4\cos\frac{m\pi}{4}\cos\frac{m\pi}{60}\sin\frac{7m\pi}{60}\right)-\sin\frac{m\pi}{12}\right]$
Col. (*g*)	36	$\frac{1}{3}\sin\frac{5m\pi}{36}\left(2\cos\frac{2m\pi}{9}-1\right)$
	72	$\frac{1}{3}\sin\frac{5m\pi}{36}\left(2\cos\frac{2m\pi}{9}-1\right)\cos\frac{m\pi}{72}$
Col. (*h*)	84	$\frac{1}{7}\left[2\sin\frac{m\pi}{7}\cos\frac{m\pi}{84}\left(2\cos\frac{3m\pi}{14}-1\right)-\sin\frac{3m\pi}{28}\right]$
Col. (*i*)	48	$\frac{1}{4}\left[\sin\frac{5m\pi}{48}\left(2\cos\frac{m\pi}{4}-1\right)-\sin\frac{m\pi}{16}\right]$
	96	$\frac{1}{4}\left[\sin\frac{5m\pi}{48}\left(2\cos\frac{m\pi}{4}-1\right)-\sin\frac{m\pi}{16}\right]\cos\frac{m\pi}{96}$

in Table 5, and the resultant winding factors were used in calculating the m.m.f. analyses which are given in Table 6. The fundamental winding factors, for 8 poles and 10 poles, are also given explicitly in Table 6 for each type of winding considered. These winding factors enable the ratio of the air-gap flux densities at the two speeds to be deduced, and the latter quantity is also shown in Table 6.

There is no possible ambiguity in the meanings of the terms 'layer factor' and 'winding factor' for those windings the whole of which are in circuit at both speeds; but for windings of which part is excluded on modulation, it is necessary to make it clear whether the layer and winding factors relate only to those conductors which are then in circuit or to the total number of conductors. The latter option—the more severe one—has been chosen in Table 6. It is as though all the conductors were always in circuit, but some of them contributed zero e.m.f. at one speed or the other. From the point of view of machine rating, this is a little pessimistic. If some conductors are cut out of circuit, it is possible to increase the current loading of the rest by an appreciable amount. When it is desired to calculate the relative flux-densities for the two speeds, however, it is clearly necessary to refer both winding factors to the same total number of conductors, even though at one speed some of the conductors are out of circuit.

Whilst these calculations of layer factor are just a little laborious, and become even more so for larger numbers of slots, they have only to be performed once for each slotting and each method of modulation. Therefore, the results can be tabulated for repeated use, just as normal spread factors have long ago been finally worked out, and never need recalculation. The labour is not one that has to be repeated with each individual machine.

If a digital computer with a suitable programme is available the layer factor of any winding arrangement can be calculated, without trouble or delay, both for the fundamental component and for as many harmonics as it is desired to consider. Now that this method of speed changing is thoroughly established, numerical results for other winding configurations of the same type could most readily be obtained by computer. The authors would emphasize, however, that, in their view, the use of alge-

Table 6

WINDINGS FOR POLE-AMPLITUDE MODULATION: NINE ALTERNATIVE TYPES FOR 8/10 POLES. RELATIVE M.M.F. AMPLITUDES, WINDING FACTORS AND FLUX DENSITIES

Types of winding		(a)	(b)	(c)*	(d)*	(e)*	(f)	(g)*	(h)	(i)*
m	Pole numbers of modulated winding	48 slots(2) modulated 2-2-2-0 2-2-2-0 or 72 slots modulated 3-3-3-0 3-3-3-0	72 slots modulated 02-3-3-20 02-3-3-20	48 slots modulated 01-2-2-10 01-2-2-10	120 slots modulated 0002-5-5-2000 0002-5-5-2000	72 slots modulated 001-3-3-100 001-3-3-100	60 slots modulated 2-3-3-2 2-3-3-2	36 slots(2) modulated 1-2-2-1 1-2-2-1 or 72 slots modulated 2-4-4-2 2-4-4-2	84 slots modulated 2-5-5-2 2-5-5-2	48 slots(2) modulated 1-3-3-1 1-3-3-1 or 96 slots modulated 2-6-6-2 2-6-6-2
1	2-pole ..	24·5	24·0	7·5	3·0	10·3	28·0	12·7	3·8	2·5
5	10-pole ..	100·0	100·0	100·0	100·0	100·0	100·0	100·0	100·0	100·0
7	14-pole ..	11·6	17·0	9·2	3·4	0·7	9·3	1·7	9·1	14·8
11	22-pole ..	9·4	6·0	5·4	5·0	5·3	5·3	3·3	0·6	0·9
13	26-pole ..	7·2	8·6	9·3	8·3	8·4	7·0	7·2	5·5	5·1
	Pole numbers of unmodulated winding									
2	4-pole ..	—(3)	—(3)	—(3)	—(3)	—(3)	12·0(4)	20·3(4)	27·2(4)	31·8(4)
4	8-pole ..	100·0	100·0	100·0	100·0	100·0	100·0	100·0	100·0	100·0
8	16-pole ..	—	—	—	—	—	1·1	3·2	5·0	6·9
10	20-pole ..	—	—	—	—	—	2·4	4·1	4·5	5·1
14	28-pole ..	—	—	—	—	—	1·7	2·9	1·4	2·6
Winding factor (10-pole)		0·552(5)	0·599(5)	0·582(5)	0·552(5)	0·538(5)	0·698	0·760	0·763	0·783
Winding factor (8-pole)		0·835	0·833	0·838	0·830	0·833	0·825	0·820	0·789	0·783
Average winding factor		0·693	0·716	0·710	0·691	0·686	0·761	0·790	0·776	0·783
B_8/B_{10} (air-gap) ..		1·06	1·15	1·11	1·06	1·03	0·780	0·855	0·890	0·925
Connections (8-pole/10-pole)		Parallel star/star(6)	Parallel-star/star(6)	Parallel-star/star(6)	Parallel-star/star(6)	Parallel-star/star(6)	Parallel-star/delta(6)	Parallel-star/delta(6)	Parallel-star/delta(6)	Parallel-star/delta(6)

* Preferred designs.

(1) All these analyses are based on windings of exactly two-thirds full pitch for 8 poles. This coil pitch is essential.
(2) Integral multiplication of the slot number, without alteration of the coil grouping or method of modulation, causes second-order changes in the higher harmonics only. For example, the 26-pole field for 48 slots is 7·5%, and for 72 slots is 6·8%. Average values are given for these cases, but the differences are negligible in practice.
(3) For windings types (a)–(e), the unmodulated winding is a standard 8-pole winding, and thus contains no m.m.f. harmonics lower than the fifth harmonic of 8 poles, i.e. 40 poles ($m = 20$).
(4) For windings types (f)–(i), the whole winding is used at both speeds and all coils are identical. The unmodulated winding is an 8-pole fractional-slot winding, of irregular coil-group distribution, and thus contains additional even m.m.f. harmonics, expecially a sub-harmonic 4-pole field.
(5) The winding factors for 10 poles are calculated with reference to the total number of conductors. For windings types (a)–(e), the winding factors of the conductors actually in circuit are correspondingly higher.
(6) For windings types (a)–(e), parallel-star/star connections, only, are likely to be used, since the flux-density ratio, B_8/B_{10}, is then so nearly unity. For windings types (f)–(i), parallel-star/delta connections give a flux-density ratio B_8/B_{10} nearer to unity.

braic methods and individual mathematical working is essential in the early stages of the development of any radically new type of winding. Only by algebraic methods can the true logic of a new type of winding be properly discerned and extended. When this stage has been passed, a good deal of labour can be saved by using a digital computer for repetitive numerical work along established lines. The authors intend to do this for more complicated pole ratios and modulation sequences; for example, for 14/16 poles.

(4) SUMMARY OF DESIGNS FOR 8/10 POLES

In Table 6 are shown the m.m.f. harmonic analyses, for both speeds, for nine basically different types of 8/10-pole winding, together with the corresponding winding factors and air-gap flux-density ratios, for the methods of connection there specified. This Table may be regarded as the basic data sheet for 8/10-pole-amplitude modulation. It will be noted that parallel-star/star connection is used for all the windings in which coils are omitted on modulation, but that parallel-star/delta connection is used for the windings which give modulation by simple reversal of a half of each phase-winding. These will be seen to be the connections which give a ratio of B_8/B_{10} as near as possible to unity: for the first five types just above unity: for the last four types just below unity.

Small machines (below 5 h.p.) were built and tested to designs (a), (c), (g) and (i); and large machines (up to 100 h.p.) to designs (a), (e), (g) and (i), all the latter designs being for the greater of the two alternative slot numbers shown in Table 6. The test results are given in succeeding Sections of the paper; but, in simple terms, these results were almost uniformly satisfactory. Designs of type (a), however, gave a noise level slightly higher than the rest, and this design—the prototype one—is unlikely to find continued application in view of the more acceptable alternatives later developed.

From Table 6, it will be observed that the four irregular fractional-slot designs (f)–(i), in which the complete winding is used at both speeds, can be derived, respectively, from the four integral-slot designs (b) to (e). If, in designs (b)–(e), the slots from which the coils are removed from circuit on modulation are supposed to be wholly eliminated from the stator, these designs (b)–(e) take, respectively, the forms (f)–(i). For example, a 48-slot stator, intended to be modulated 01-2-2-10, etc., becomes compressed into a 36-slot stator, wound 1-2-2-1, etc. There are thus two distinct forms of

pole-amplitude modulation, which are suited to different types of drive.

Summarizing, it may be said that designs (*a*) and (*b*) are unlikely to be much used, even though they are quite satisfactory in operation; but that designs (*c*), (*d*) and (*e*) are completely satisfactory, especially for load torques which fall as the speed falls. Designs (*c*), (*d*) and (*e*)—with slotting repeated if necessary—can be used for machines with 2, 3, 4, 5, 6, 8, 9, 10 or 12, etc., slots per pole per phase; in fact, for almost any size of motor. All these designs give a close approach to the full frame rating for the motor at the higher speed, and approximately 70% of the full frame rating at the lower speed. The only reduction of rating at the higher speed arises from the requirement of two-thirds full-pitch coils. In any event, some chording is common enough and the reduction may well be marginal.

All four designs (*f*)–(*i*) give satisfactory performance, but it is probable that the two designs (*g*) and (*h*) are to be preferred, since they have the lowest average values of subharmonic. For the same frame size, at the higher speed, design (*g*) gives about 85% of the output given by the corresponding designs (*c*), (*d*) or (*e*); but, on the other hand, the output at the lower speed is increased by about 25% by the change from design (*c*), (*d*) or (*e*) to design (*g*).

As will be seen from the tests described below, a typical frame, intended for 70 h.p. when wound with a standard 8-pole winding, gave 70/35 h.p. at the two speeds for design (*a*), but 60/45 h.p. for design (*g*). Designs (*f*)–(*i*) are therefore appropriate for approximately constant-torque applications; but designs (*a*)–(*e*) are best for applications such as fan drives, where the power output falls rapidly as the speed falls. In general, the respective power ratings at the two speeds are approximately 100 and 70% of the corresponding single-speed ratings, for the first type of design; and approximately 85% of the corresponding single-speed ratings, for both speeds, for the second type of design.

It ought to be added that the comparison of ratings between a 2-speed double-wound machine and a 2-speed machine with one winding of this new type, is most favourable to the single-winding machine when it is to be wound for a high voltage. The improvement in space factor, arising from the need for only one lot of high-voltage insulation, can then be very considerable, and the ratings are correspondingly increased.

(5) DOUBLE REPETITION OF MODULATED WINDINGS

By this term is meant the construction of such a winding as one for 16/20 poles; which consists, of course, of two identical 8/10-pole components, each occupying one half of the perimeter of the machine. A 16/20-pole machine wound according to design (*c*) of Table 6, for example, would require a stator with 96 slots.

The extension from 8/10 poles to 16/20 poles does, however, enable one simplification to be effected, in relation to those designs, such as (*a*)–(*e*) in Table 6, in which certain coils are omitted on modulation. It was explained in Section 5 of Reference 1 that these coils have to be wound with half the number of turns, of twice the wire cross-section, compared with the rest of the coils. Further, these coils have to be connected in series so that there is no closed circuit around them after modulation, when they are omitted from circuit. If they were then connected in parallel, there would be heavy circulating currents around the closed circuit. Alternatively, the closed circuit could be opened on modulation, but this would require three extra control leads.

While this winding requirement is easily complied with, and really requires only a little extra supervision in manufacture, it can be avoided altogether in doubly repeated windings. The two coils, or coil groups, which are to be omitted from each 8/10-pole component of the winding, for example, can then be wound like the rest; and all are connected in series for each 8/10-pole component of the winding. The two resultant series sections, for the two 8/10-pole components, are then connected in parallel with one another. The voltages in these two series-connected sections will be cophasal for both pole numbers, since the first half and the second half of the complete winding are, of course, identical, and are situated at opposite ends of a diameter of the machine. Hence two parallel paths are obtained through those coils which are to be omitted on modulation, but the voltages which are induced in this closed circuit, after modulation, are algebraically zero in total, and there is no circulating current.

Comparable simplifications, enabling all the coils of the machine to be wound identically, can be effected for any doubly-repeated winding arranged for pole-amplitude modulation; or indeed, for a four-times-repeated winding, if this were ever required to be built.

In large low-voltage machines, where the number of turns per coil will be low, and may even be unity, the requirement to make some coils of half the turns number and double thickness may become difficult, or impossible, to achieve. In machines which incorporate a doubly-repeated modulation cycle, the difficulty can be readily overcome in this way. Since large machines are commonly multipolar, the modification discussed in this Section is likely to be applicable to a considerable proportion of those cases where difficulty arises in making coils of half the turns number and double thickness.

(6) TEST RESULTS FOR SMALL MACHINES

(6.1) Tests on Motor with Uniform Integral-Slot Winding

(6.1.1) No-Load Tests.

The first machine upon which tests were performed was the same as that discussed in the earlier paper,[1] this having a 48-slot stator wound for 3 phase, 8 poles, two-thirds full pitch. The modulation sequence previously used was 2–2–2–0; whereas the winding was here modulated 01–2–2–10. The two corresponding 10-pole magnetizing curves are shown in Fig. 9.

Since the winding factor has been increased from 0·552 to 0·582 by changing the method of modulation (see Tables 5 and 6), the flux for a given voltage is reduced in this ratio, and the magnetizing current for a given voltage in the square of this ratio; i.e. in the ratio $(0\cdot552/0\cdot582)^2 = 0\cdot90$. An inspection of Fig. 9 will show that this is broadly correct.

With the new method of modulation, there is, however, an appreciable reduction in the no-load power loss, as shown by reference to Fig. 10. A part of this reduction in power loss is due to the 5% reduction in flux, for the same applied voltage; but a comparison between the losses at, for example, 420 volts with the new method of modulation, and at 400 volts with the old method, shows that an appreciable net reduction in no-load power loss results from the adoption of this new method of modulation.

(6.1.2) Short-Circuit Tests.

The short-circuit impedances per phase were found to be 15·4 ohms (8-pole) and 31·0 ohms (10-pole) for the original method of modulation, 2–2–2–0. For the new method of modulation, 01–2–2–10, the 10-pole short-circuit impedance was found to be virtually the same as before. Corresponding resistances were 7·3 and 14·8 ohms, the alteration in modulation method again making no sensible change.

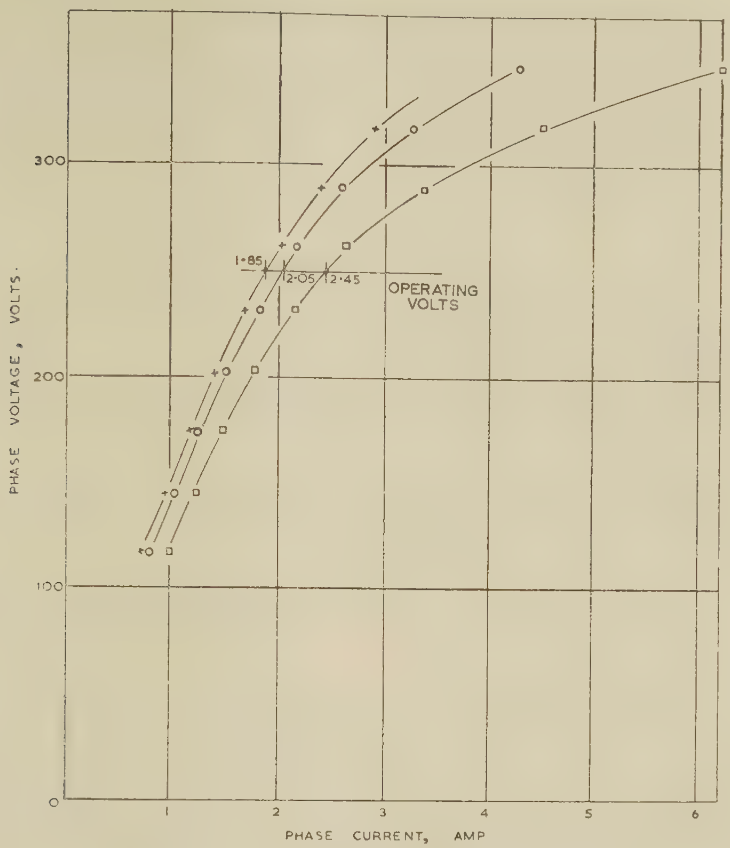

Fig. 9.—8/10-pole-motor. Magnetizing curves.

□ □ □ 8-pole parallel-star.
○ ○ ○ 10-pole series-star (modulated 2220–2220).
× × × 10-pole series-star (modulated 012210–012210).

For both methods of modulation, therefore, the ratio between the pull-out powers, P, for the two speeds is given by

$$\frac{P_8}{P_{10}} = \frac{3\left(\frac{V_L}{\sqrt{3}}\right)^2}{2(7\cdot 3 + 15\cdot 4)} \times \frac{2(14\cdot 8 + 31\cdot 0)}{3\left(\frac{V_L}{\sqrt{3}}\right)^2} = \frac{45\cdot 8}{22\cdot 7} = 2\cdot 02$$

It is thus clear that the pull-out torque is reduced approximately in the ratio $2\cdot 02 \times 0\cdot 8 = 1\cdot 62$, when the speed is changed; but since the continuous load torque is likely to be reduced in a greater ratio, this performance is quite acceptable.

(6.1.3.) Full-Load Tests.

As recorded in the original paper,[1] the test rating of this machine at 440 volts input was 1·6 h.p., using the original method of modulation, 2–2–2–0. At the same voltage, the same machine with the new method of modulation, 01–2–2–10, had a test rating of 1·65 h.p. The flux at this voltage with the latter method of modulation is, however, less than with the former; and, in order to make the tests comparable, the machine was tested again with the original method of modulation, at an appropriately reduced voltage of 417 volts. The continuous rating was then found to be 1·45 h.p., which is almost exactly what would have been expected, since $(417/440)^2 \times 1\cdot 6$ h.p. $=$ $1\cdot 44$ h.p. It is therefore clear that, on any reckoning, the rating is appreciably improved by the new method of modulation. Since the new method can be applied as readily and simply as the old one, the latter can be regarded as obsolete for all practical purposes.

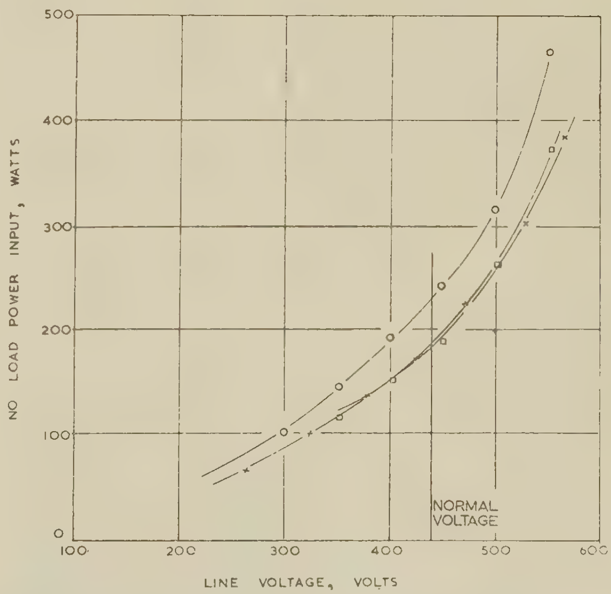

Fig. 10.—8/10-pole motor. No-load power/voltage curves.

□ □ □ 8-pole parallel-star.
○ ○ ○ 10-pole series-star (modulated 2220–2220).
× × × 10-pole series-star (modulated 012210–012210).

In Table 6, columns (*a*) and (*c*), the m.m.f. content is shown for both methods of modulation for 48 slots, and also, in columns (*d*) and (*e*), for two comparable methods of modulation which can be applied, respectively, to 120 and 72 slots; i.e., modulation by the sequences 0002–5–5–2000 and 001–3–3–100. It is clear on theoretical grounds that any of the three last methods is to be preferred to the original method given in column (*a*).

(6.2) Tests on Motor with Irregular Fractional-Slot Winding

(6.2.1) No-Load Tests.

In Section 2.4 the winding of a 36-slot stator with a fractional-slot winding of an unusual type was discussed, and the clock diagram of Fig. 3 shows the distribution of the phase bands. A connection diagram for the same machine is given in Fig. 11, the coil groups being brought to separate terminals in the experimental machine.

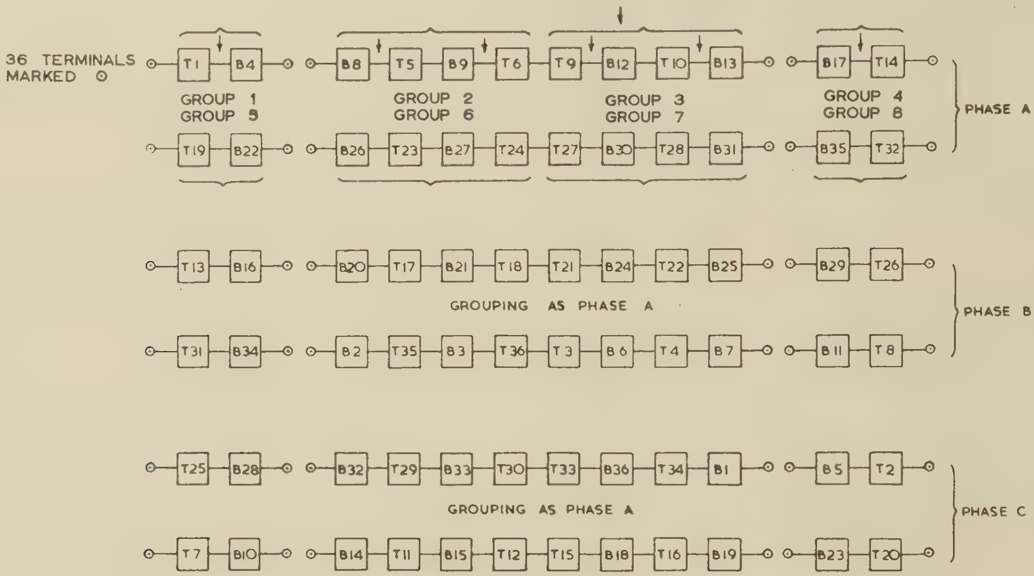

Fig. 11.—Connection diagram for experimental 8/10-pole motor.

36 coils, each of 40 turns of No. 20 s.w.g. Pitch: 1–4 (three slots). T: Coil sides in tops of slots, numbered consecutively.
B: Corresponding coil sides in bottoms of slots.

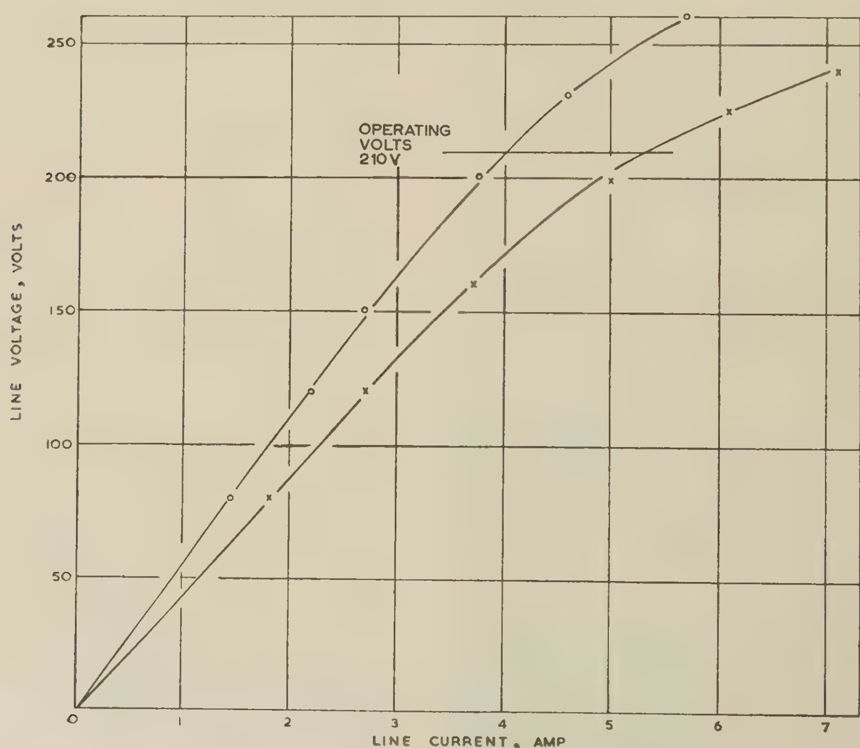

Fig. 12.—Magnetizing characteristics for 36-slot 8/10-pole motor.

○ ○ ○ 8-pole parallel star connection.
× × × 10-pole series delta connection.

The magnetizing curves for this machine, both unmodulated (8-pole) and modulated (10-pole), are given in Fig. 12, and the no-load power/voltage curves for both pole numbers are shown in Fig. 13. This last Figure also shows the residual power consumption for windage and iron losses at both speeds on no load, after the calculated stator copper losses were subtracted from the measured total input power. The magnetizing curve and the no-load power/voltage curve for a standard 8-pole machine, wound in the same frame for the same voltage and flux density, were also available for comparison. The 8-pole winding factor of the special motor, as found by the methods of Section 3.2 and recorded in Table 6, is almost identical with that of the standard motor; and the two 8-pole magnetizing curves, as would be expected, were indistinguishable. The 8-pole no-load power/voltage curve of the special motor was just observably different from that of the standard motor, the no-load power being about 5% greater for the special motor. It therefore appeared that the no-load performance of the machine as an 8-pole motor was not impaired to any sensible degree by the use of an irregular fractional-slot winding, instead of a regular fractional-slot winding.

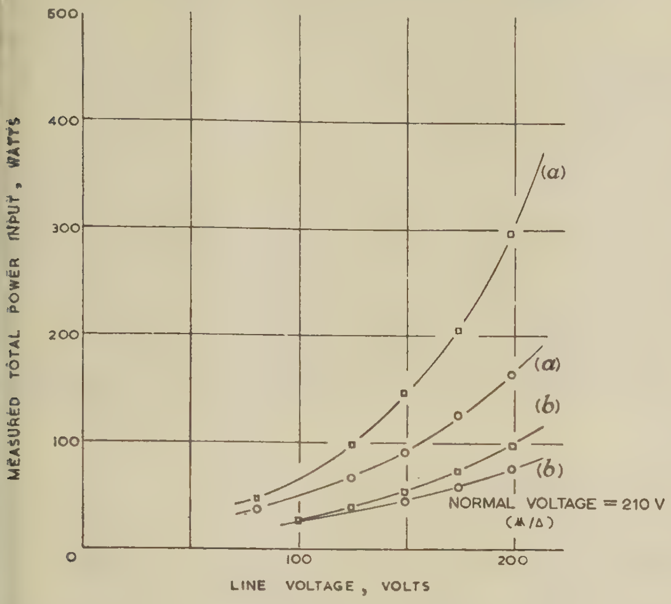

Fig. 13.—No-load power/voltage curves for 8/10-pole motor, with fractional-slot winding.

○ ○ ○ 8-pole parallel-star.
□ □ □ 10-pole series-delta.
(*a*) No-load power input (*P*).
(*b*) Input *W*, less calculated stator I^2R losses.
36 slots. Coil grouping 1–2–2–1—1–2–2–1.
Coil pitch 1–4 = Two-thirds full pitch for 8 poles.

When the motor was operated on no-load in the 10-pole connection there appeared to be a considerable increase in no-load power, as shown in Fig. 13; and at first, this was thought to be evidence of heavy harmonic losses. Simple calculation of the stator I^2R losses showed, however, that the bulk of the increase was due to increase in the stator copper losses, arising from the higher flux and magnetizing current in the 10-pole connection. This consideration would, of course, tend to reduce the optimum operating voltage, and thus to lower the rating of the machine; but if these I^2R losses are subtracted from the total power input it will be found that the increase in no-load power loss due to harmonics, when switching from 8-pole to 10-pole operation, is very small. The net no-load power loss, after subtraction of the stator copper loss, must include all losses due to harmonics, and this power loss is plotted for both speeds in Fig. 13; and, having regard to the fact that the flux density for a given voltage has been increased by 15% on switching to 10 poles, it is very doubtful whether any account at all need be taken of possible extra harmonic losses. It is true that the windage element of loss will be a little greater for 8-pole working, but in both cases this is so small as to be difficult to determine. These no-load tests, taken together, tended to show that this unorthodox machine gave a no-load performance very similar to that of a standard machine, at both speeds.

(6.2.2) **Short-Circuit Tests.**

The short-circuit impedances per phase were found to be 8·27 ohms (8 pole) and 29·0 ohms (10 pole), the corresponding effective resistances being 3·36 and 12·4 ohms. Apart from inter-phase leakages and alterations in stray loss, the resistance and impedance per phase would be expected to alter in the ratio 1 : 4, when changing from parallel-star to series-delta connection. In fact, therefore, the 10-pole values are a little lower than might have been expected. The ratio between the pull-out powers, *P*, for the two speeds, for parallel-star/delta connection, is given by

$$\frac{P_8}{P_{10}} = \frac{3\left(\frac{V_L}{\sqrt{3}}\right)^2}{2(8\cdot 27 + 3\cdot 36)} \times \frac{2(12\cdot 4 + 29\cdot 0)}{3V_L^2}$$

$$= 1\cdot 18$$

Having regard to the fact that the speed for 8 poles is 25% greater, it is clear that the performance of this unusual machine in the modulated connection is in no way limited by the leakage reactance, and that overload torques of normal magnitudes can be expected. It should be added that the actual overload torques for both speeds, and for a normal machine wound for 8 poles in the same frame, were rather below normal. This was simply because the frame used for the test motor was unduly long and narrow, being intended by the manufacturer for 4-pole operation.

(6.2.3) **Full-Load Tests.**

As will be seen from Table 6, columns (*f*)–(*i*), parallel-star/delta connection is normally appropriate for irregular fractional-slot 8/10-pole windings, modulated by simple reversal of half phase windings. This connection gives the nearest possible approach to unity for the various values of the ratio (B_8/B_{10}), but the flux densities are a little greater at the lower speed, and the voltages are accordingly fixed by reference to that speed. Inspection of the magnetizing curves in Fig. 12 will show that 210 volts was a reasonable test voltage for this small machine at 10-pole speed, and this was accordingly taken as the rated voltage of the test machine.

The performance obtained at this voltage was as follows:

8-pole: 1·90 h.p.: 0·71 power factor: 73·0% efficiency
10-pole: 1·05 hp.: 0·55 power factor: 62·0% efficiency

Another standard machine, wound normally for 8 poles in an identical frame, gave test results indistinguishable, within normal limits of accuracy, from the results for this machine at 8-pole speed. These somewhat low figures for efficiency and power factor are therefore in no way a criticism of the unusual winding; they arise partly from the use of a frame not well proportioned for 8/10 poles, and even more from the small size of the machine. As is well known, small machines normally give performances much inferior to those of comparable large ones, and the above figures closely resemble those for standard industrial single-speed induction motors of comparable ratings.

(7) TEST RESULTS FOR LARGE MACHINES

Any lingering disquiet which remained about the performance figures for these new types of motor was wholly removed by the test results obtained on four much larger machines, made and tested in the works of two major manufacturers, to the authors' designs. Designs of type A are those given in columns (*a*)–(*e*) of Table 6, which omit coils on modulation but are of standard form before modulation; designs of type B are those given in columns (*f*)–(*i*) of Table 6, where all the coils are used both before and after modulation. The test results were uniformly excellent, and are summarized in Table 7. The rather poorer performance obtained from several small laboratory machines, all of less than 3 h.p., was thus shown to have arisen solely from the miniature scale of the original tests and from the unsuitable proportions of some of the motor frames used for the tests.

(7.1) Rated Outputs of Test Machines

The outline design particulars of the four larger machines were as given in Table 7. All these machines were given full load/temperature tests at both speeds, and Table 7 shows the test outputs for the four machines in comparison with the rated outputs of the same frames when wound with single-speed windings for one or other speed. The percentage ratings of 100%/70% for designs of type A, and 85%/85% for designs of type B, referred to in Section 4, are amply confirmed.

Careful observations of rotor temperatures were made in each case, because, from theory, it seemed possible that the usual rotor losses would be exceeded when the rotors were submitted to unusual stator m.m.f.'s. There were no sensible differences in rotor temperatures from those normally found in the same machine frame at the same load, and any increase in rotor losses was certainly too small to be of any practical importance. The rotor slotting for each motor is given in Table 7, the slot number in every case being that used as a standard for the particular motor frame and stator slotting. It may be that, under some unusual conditions, a modulated winding will react unfavourably with a particular rotor slotting, but no example of this has yet been encountered in over 20 different machines, small and large.

(7.2) Starting Torques and Currents, Maximum Torques, Efficiencies and Power Factors

In addition to carrying out full-load temperature tests, complete no-load and short-circuit tests were performed for all four machines at both speeds. It is clearly undesirable to reproduce all these results in detail, but it should be stated at once that in all cases there were no abnormal iron losses on no load. Further, the theoretical starting torque, as calculated from the short-circuit test, was in every case fully satisfactory. The usual test-bed readings enabled full-load efficiency and power factor to be deduced for each machine, and these are given in Table 7. The starting torques were also measured, at reduced voltage and extremely low speed, using a brake; and the starting currents were obtained from the oscillograph tests, which are discussed in Section 7.4. The ratios between maximum and starting torques were also obtained from these oscillograms.

Table 7

BASIC SPECIFICATION AND TEST RESULTS FOR FOUR EXPERIMENTAL 8/10-POLE MOTORS

Stator slot number, coil pitch and modulation sequence	Design type	Pole number	Single-speed frame outputs	Test output of motor with new winding	Full-load efficiency	Full-load power factor	Starting torque	Starting current	Maximum torque
			h.p.	h.p.	%		× full load torque	× full load current	× full-load torque
72 slots; pitch 6 slots; 3–3–3–0 reverse 3–3–3–0	Type A; col. (*a*), Table 6; 76 rotor slots	8	70	70	90·5	0·86	1·58	5·48	2·55
		10	50	35	86·6	0·77	1·12	4·23	2·15
72 slots; pitch 6 slots; 001–3–3–100 reverse 001–3–3–100	Type A; col. (*e*), Table 6; 88 rotor slots	8	100	100	89·4	0·82	1·33	3·82	1·85
		10	70	50	85·2	0·71	1·16	3·47	1·72
72 slots; pitch 6 slots; 2–4–4–2 reverse 2–4–4–2. (Same frame as first machine)	Type B; col. (*g*), Table 6; 76 rotor slots	8	70	60	89·7	0·86	1·34	4·05	2·25
		10	50	45	88·8	0·78	1·26	4·30	2·02
96 slots; pitch 8 slots; 2–6–6–2 reverse 2–6–6–2	Type B; col. (*i*), Table 6; 80 rotor slots	8	45	38	90·3	0·86	2·10	5·15	3·00
		10	32	30	88·0	0·76	1·58	5·50	2·50

(7.3) No-Load Current and Power Curves

As examples of the kinds of test result obtained on no load, the no-load current and power curves for a machine wound according to column (*g*) of Table 6, which is the preferred design, are given in Fig. 14. The no-load current and power at the 10-pole speed are only a little greater than at the 8-pole speed; and, having regard to the fact that the flux density for 8 poles is 0·855 times that for 10 poles, as shown in Table 6, this is completely reasonable. The measured ratio, I_8/I_{10}, between the initial slopes of the magnetizing-current curves is equal to 0·72, which must be compared with the theoretical ratio of 0·73. This is a very close degree of coincidence, and it shows that the usual method of calculation, by which the ratio of the magnetizing ampere-turns per pole for the two speeds is equated to the air-gap flux-density ratio, for a given voltage, is just as applicable to these novel types of winding as to more orthodox ones.

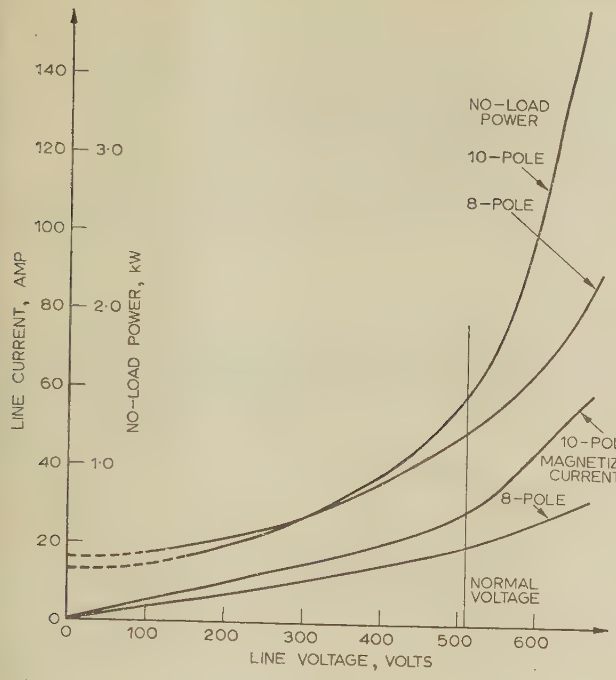

Fig. 14.—No-load current and power for 8/10-pole motor, 60/45 h.p., wound 2–4–4–2, etc., in 72 slots.

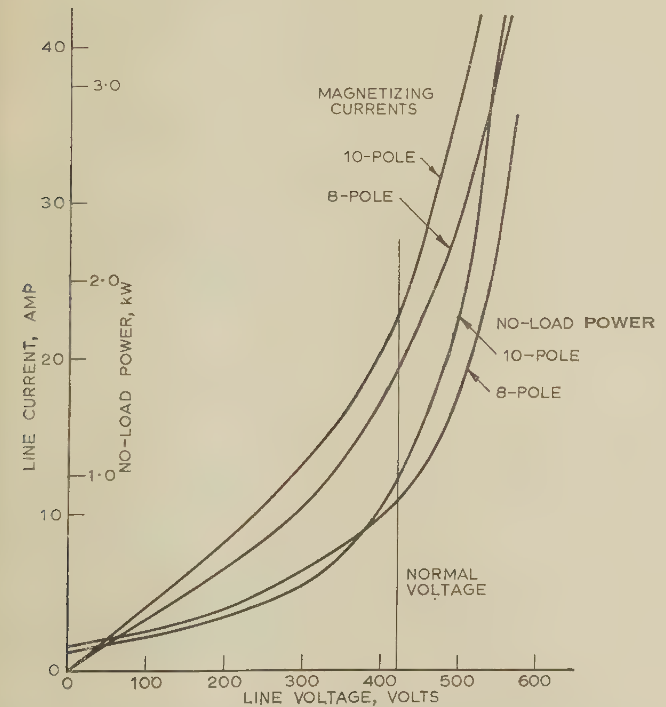

Fig. 15.—No-load current and power for 8/10-pole motor, 38/30 h.p., wound 2–6–6–2, etc., in 96 slots.

The no-load test results on a machine wound in 96 slots, according to column (*i*), Table 6, are also of exceptional interest, because this represents the most extreme case of unequal coil grouping, in each phase winding which is likely to be encountered, the grouping here being 2–6–6–2—2–6–6–2.

The no-load current and power curves for this last machine are shown in Fig. 15, the 8-pole and 10-pole curves approaching more closely than in Fig. 14, as would be expected with closer approximation of the flux-density ratio to unity, the value here being 0·925. The measured ratio (I_8/I_{10}) between the initial slopes of the magnetizing current curves is 0·81, as compared with the calculated value of 0·85. It is clear that, to a very close approximation, all these machines conform with classical theory, although the winding factors involved are much more difficult to calculate than the winding factors of conventional machines.

(7.4) Acceleration Tests

In view of the possibility of parasitic torques, particular attention was paid to the accelerating characteristics of these machines.

Dynamic acceleration tests were performed on all the four large test machines, the details of which are given in Table 7. Tests were performed for both pole numbers for all the machines; although the results in the unmodulated condition were not expected to show any unusual features for two of the machines, which were made to designs (*a*) and (*e*) of Table 6, these machines thus being of the standard type when unmodulated. Every test consisted of taking an oscillograph record of speed and acceleration, together with starting current, while the motor was 'plugged' on full voltage from full-speed reverse to full-speed forward.

Excellent accelerating characteristics, fully up to those for a normal single-speed induction motor, were obtained for all four machines, for both synchronous speeds. The most interesting of the eight oscillograph records is the one which shows the most unusual features, and this is reproduced in Fig. 16. It records an acceleration test for 10-pole working for a machine to design (*a*) of Table 6, which is the machine containing the largest proportion of harmonic fields amongst the four machines tested.

This oscillogram is almost a textbook classic, and thus deserves a few comments. It has already been shown in an earlier paper[1] that the possible crawling harmonics to be considered in modulated windings are not integral multiples of the fundamental field, but integral multiples of a 2-pole field, the machines really being 2-pole machines operating on harmonics of high value. The point can be exemplified by considering Fig. 16. Since the fundamental field of an 8/10-pole motor in the 10-pole connection is, logically, the fifth harmonic of 2 poles, it follows that the principal crawling harmonics in the forward direction are the 11th and 17th, and that the principal crawling harmonics in the reverse direction are the 7th and 13th. As shown in Table 6, these harmonics are all of appreciable magnitude in a motor to design (*a*). The synchronous speeds of the forward harmonics are 272 and 177 r.p.m.; the synchronous speeds of the reverse harmonics are 428 and 231 r.p.m. Both of the reverse harmonics can be very clearly seen, and have produced appreciable dips in the negative part of the speed/torque curve. The forward-rotating 11th harmonic can also be seen, although fortunately, its magnitude is certainly not sufficient to cause any trouble. The 17th harmonic is not visible. In spite of these observable harmonic effects, the acceleration curve of Fig. 16 would, of course, be thoroughly acceptable in practice.

The acceleration curve of the machine made to design (*e*) showed only one irregularity, the 13th backward-rotating harmonic causing a very slight dip at the point concerned in the negative part of the curve. Apart from this, the curve was of completely standard form, and therefore very satisfactory. Reference to Table 6 will show that the 26-pole field (the 13th harmonic) in design (*e*) is relatively larger than the other harmonic fields, and the fact that this was the only harmonic of which the effect was discernible in the speed/torque curve is completely in accordance with the m.m.f. waveform analysis. It is now very doubtful whether design (*a*) would ever be pre-

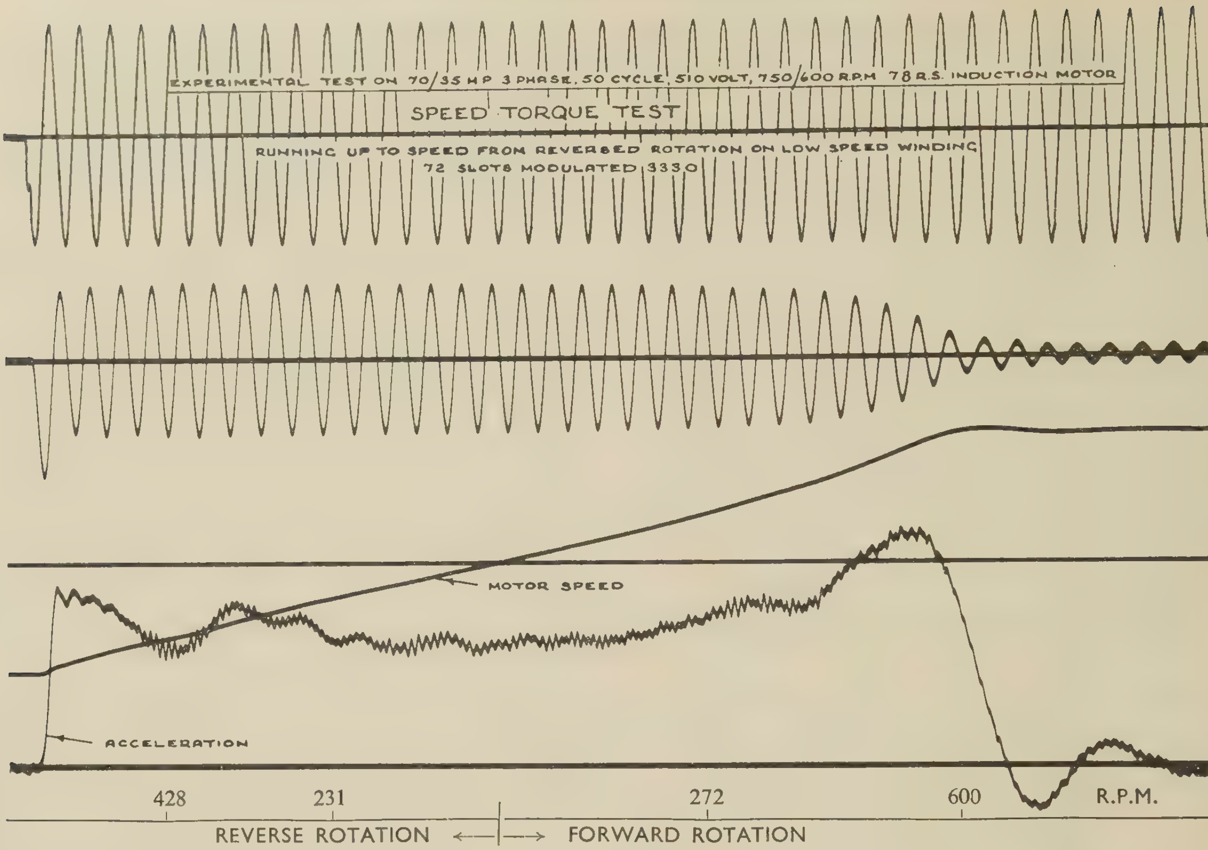

Fig. 16.—Current, speed and acceleration curves for reverse plugging, in 10-pole connection, of first large 8/10-pole motor.

ferred in practice to design (*e*), but design (*a*) was devised first, and the experimental acceleration curve of design (*a*) has furnished a most elegant confirmation of the whole theory. This variety of harmonic torque is new in principle, compared with the kinds normally experienced, and it is most satisfactory that the first acceleration curve taken should have shown these effects so convincingly.

For designs (*g*) and (*i*) it was absolutely necessary to take acceleration curves for both speeds, since the winding was not a standard for either connection. The curves were all satisfactory, and well in accordance with accepted performance standards, and they require no further comment beyond this simple statement. There are no grounds whatever for supposing that the unusual m.m.f. waveforms of these machines are likely to cause bad torque characteristics. All the evidence from the machines so far built is entirely favourable.

(7.5) Noise Level

Direct noise measurements were actually made on two of the machines, but such results are always hard to evaluate, and tests were difficult because the slight magnetic noise was completely drowned by normal fan, ventilation and other mechanical noises.

Eventually, arrangements were made to run every machine covered by Table 7 on test out of normal working hours, against a dead quiet background. As a result, it can be stated specifically that, under these stringent conditions, all the machines were satisfactorily quiet both on no load and on load; and, indeed, they compared favourably with several standard machines of similar size and rating which happened to be available on test, and were started up for comparison. The second and third machines in Table 7, the preferred designs, were almost abnormally quiet magnetically, even with excess voltage and current; and the authors have formed the view which they are considering further, that machines after modulation are inherently quieter than unmodulated machines. At the least, it is now possible to state with certainty that 2-speed single-winding machines of this new type are as quiet as 2-speed machines with two separate windings.

(8) ACKNOWLEDGMENTS

The authors wish to record their appreciation of the co-operation of Associated Electrical Industries, Ltd., and Lancashire Dynamo and Crypto, Ltd., in building and testing large machines. They also wish to express to Mr. A. G. Williamson, Chief Engineer of the Motor and Control Gear Division of Associated Electrical Industries, Ltd., and to the management of Lancashire Dynamo and Crypto, Ltd., their thanks for permission to publish the test results of the large machines. Warm personal gratitude is also due to Messrs. A. F. M. Ashworth and H. Sterling for their personal interest and enthusiasm.

Acknowledgment is also due to the Brush Electrical Engineering Co., Ltd., who have enabled one of the authors (W. F.) to continue to work at Bristol on the development of speed-changing inventions.

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WATER-TURBINE-DRIVEN INDUCTION GENERATORS

By C. L. C. ALLAN, B.A., M.I.C.E., Member.

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SUMMARY

The paper outlines the main differences between motor and generator operation and considers the excitation of generators, in particular when provided by system capacitance. Results are given of tests on machines to ascertain the practical effects of uncontrolled excitation and switch-in currents. Information is given about some advantages arising from the absorption of reactive current produced by long transmission lines and the reduction in voltage drop on lines delivering power.

The equipment necessary for control, protection, starting and synchronizing is described, as well as technical matters concerned with drying out windings and checking phase rotation.

Three typical schemes are described and a list of existing and planned installations is given.

An Appendix deals in more detail with the theoretical values of the equivalent circuit, torque and power factor, as well as circle diagrams.

LIST OF SYMBOLS

V = Voltage of system.
V_s = Voltage induced in stator.
V_r = Voltage induced in rotor (referred to stator).
I_s = Current in stator.
I_{nl} = No-load current in stator.
I_r = Current in rotor (referred to stator).
I_m = Current for magnetization.
Φ_m = Magnetic flux.
ϕ_s = Angle between system voltage and stator current.
ϕ_R = Angle between rotor voltage and rotor current.
R_s = Resistance of stator.
R_R = Resistance of rotor.
R_R/S = Resistance of rotor (referred to stator).
X_s = Reactance of stator.
Z_s = Impedance of stator.
S = Slip.
X_r = Reactance of rotor (referred to stator).
Z_r = Impedance of rotor (referred to stator).
Y_m = Magnetizing admittance.
R_m = Equivalent series resistance of Y_m.
X_m = Equivalent series reactance of Y_m.
P_R = Power input to rotor.
f = System frequency.

(1) INTRODUCTION

Induction generators are less expensive than synchronous machines and have simpler control and auxiliary arrangements. Where load and excitation conditions are suitable, good use can be made of them. Problems concerning excitation, magnetizing current and power factor of generation can usually be satisfactorily dealt with. In the North of Scotland there are machines ranging in size from 30 kW to 5 MW, and installations totalling over 20 MW, with an annual production of 64 × 10^6 kWh are in use; further schemes are under construction totalling 7 MW and 10 × 10^6 kWh.

Mr. Allan is with the North of Scotland Hydro-Electric Board.

(2) THEORETICAL CONSIDERATIONS

(2.1) Vector Diagram and Comparison with Motor

The differences between motor and generator operation can be seen by reference to Fig. 1, where vector diagrams are shown with the usual arrangement of rotor quantities expressed in equivalent stator values.

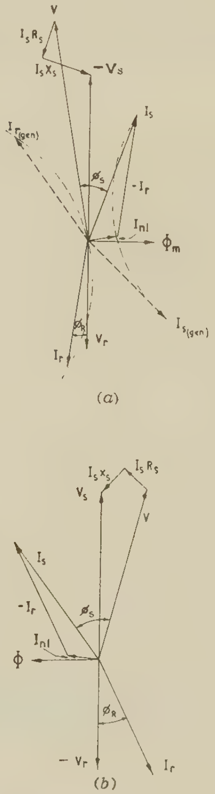

Fig. 1.—Vector diagram.
(*a*) Motor.
(*b*) Generator.

Fig. 1(*a*) is the normal vector diagram for an induction motor operating below synchronous speed and with the stator current lagging on both system and stator voltages.

If the shaft speed is now raised by the application of power to it, the slip, induced e.m.f. and rotor current will all begin to decrease and finally become zero at synchronous speed, after the rotor current has moved along the dotted circle. At this point the stator copper and iron losses, together with the magnetization, are fed by the supply system, while the friction and windage losses are supplied from the source of power, e.g. a turbine. The rotor losses are zero. Further increase of speed above synchronous will cause the rotor e.m.f. and current to reappear but in phase opposition to the previous position. Likewise the stator current vector moves to a new position. The shaft power input is converted into electrical energy and supplies the machine's

losses and its power output. The magnetizing current increases somewhat but remains constant in direction, being still supplied by the external system.

Fig. 1(a) is based on an internal view of the machine, usually adopted for the motor vector diagram. The normal way to draw a generator vector diagram is to change the viewpoint to an external one, and the result is the redrawn vector diagram of Fig. 1(b) in which the stator voltage and current occupy positions leading the system voltage.

(2.2) Excitation

An induction machine, whether motor or generator, requires reactive current for magnetization from an external source. This source is usually synchronous generating plant elsewhere on the electrical system, but it may, in certain cases, be obtained from capacitance inherent in the system.

The currents produced with varying voltage and frequency on a system comprising inductance and capacitance in parallel are shown in Fig. 2. At any frequency, if the capacitive current

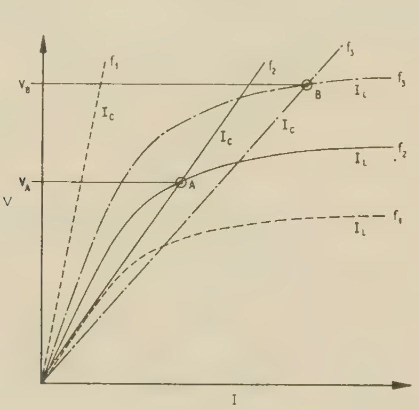

Fig. 2.—Voltage/current characteristics of generator and line at various frequencies.

I_c exceeds the inductive current I_L, the system will be self-exciting. At the lowest frequency f_1, I_c is always less than I_L, and so the system will not excite. For frequencies f_2 and f_3, stable points of excitation will exist at A and B, respectively.

It is therefore necessary, when installing an induction generator, to consider the voltages which could be attained at frequencies up to machine runaway if it were left connected to a capacitance.

(3) PRACTICAL CONSIDERATIONS

(3.1) Excitation from System Capacitance

(3.1.1) Reason for Excitation.

If a generator when running on load becomes disconnected from its power delivery point but remains connected to an overhead line or cable, a self-exciting system can be created, if the line or cable provides enough capacitive current. There will be no real control of the voltage because the loss of load will lead to a rise of speed and frequency on the self-exciting electrical system. Such conditions are particularly likely to occur with small machines because the ratio of system capacitive current to machine magnetizing current will be large.

(3.1.2) Calculations for 350kW Machine.

Calculations of self-excitation conditions have been made in a number of cases, and a series of tests under actual operating conditions have been carried out on a small-compensation water machine at Quoich Dam. The main features of the generator are as follows:

Power	350kW (3-phase)
Frequency	50c/s
Voltage	415 volts
Full-load current	577amp
Power factor	0·84
Synchronous speed	750r.p.m.
Full-load speed	762r.p.m.
Runaway speed	1750r.p.m.
Full-load slip	1·6%
Turbine	Horizontal Francis.
Load control	Hand-operated guide vanes.
Shut-down devices	Over-voltage, under-voltage and over-speed protection energize a solenoid which operates a hydraulic cylinder to close the guide vanes.
Step-up transformer	500kVA. Ratio 0·433/33kV
Main switch	Oil-immersed 0·433kV
Length of 33kV line	15 miles.
Phase capacitance of 33kV line	0·225μF

Fig. 3 shows the connections of the machine, transformer and 33kV system.

Fig. 4 shows the generator and transformer magnetization curve and line capacitance current referred to the low-voltage side of the transformer. Both magnetization and line-charging currents are shown for a range of frequencies up to twice normal. The intersections of the curves and lines at each frequency are points where excitation should occur.

Excitation would be expected to occur just below 50c/s at a voltage below normal and at successively higher voltages with rising frequency. In Fig. 5 are shown (a) the speed/voltage curve, (b) the speed/current curve for the current on the transformer high-voltage side, and (c) the speed/current curve for the current at the generator. These curves are derived from Fig. 4. Curve (c) has been included because the actual measurements of voltage and current were made at the machine terminals, and so the practical or test equivalent of curve (b) was not obtained.

(3.1.3) Tests on 350kW Machine.

From Fig. 3, which shows the main test circuit, the measuring arrangements can also be seen. Because of the over-voltages to be expected, connections from the 33kV line to consumers were temporarily disconnected.

(a) *Steady State.*—With the 33kV switch open, the machine was run at a series of different speeds and readings were taken of the machine phase-to-phase voltage, the machine phase current and the percentage gate opening. The results of the values obtained for voltage and current have been plotted in Fig. 5, and they show reasonable agreement with the calculated values of curve (c).

(b) *Dynamic Conditions.*—A number of tests were made with the machine running initially at normal speed. The effects of load disconnection were tried and also switching the transformer and 33kV line on and off. Voltage, current and speed were recorded on an oscillograph. Excitation when switching on the line to the running machine and die-away voltage on disconnection of the line took place in the way which would be expected. Disconnection of load while the machine remained connected to the 33kV line caused over-voltage as well as excess speed, and various tests were made with automatic shut-down taking place, either with over-speed or over-voltage protection.

Fig. 6(a) shows the build-up of voltage with the 33kV line switched on to the running machine. No change was made in the turbine gate opening, which is the reason for the small drop in speed, caused by the rise of losses in the generator and trans-

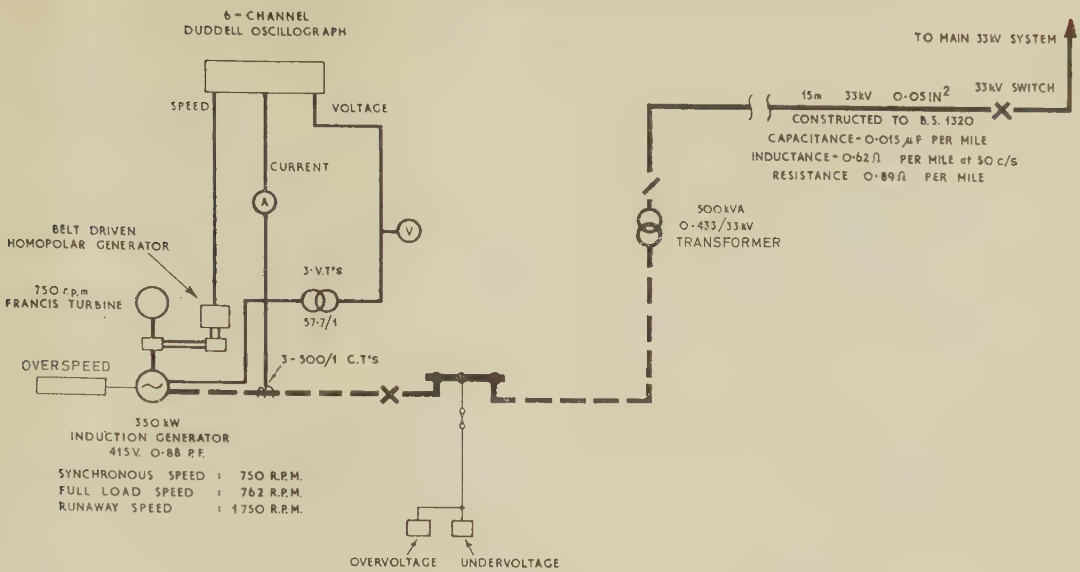

Fig. 3.—Schematic of the excitation tests for the Quoich induction generator.

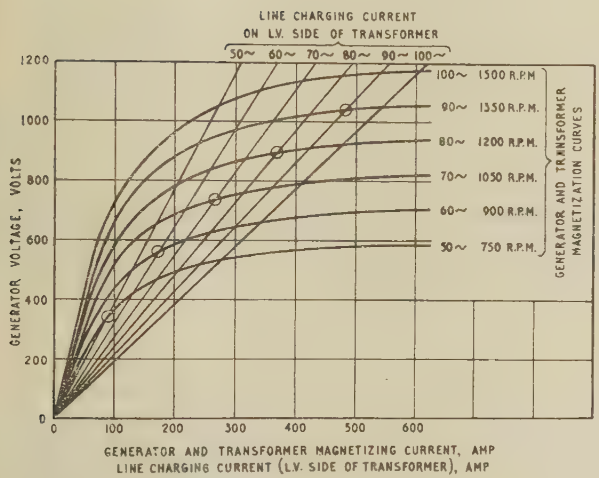

Fig. 4.—Quoich induction generator magnetization and line-charging-current/voltage curves at various frequencies.

Transmission line	33 kV
Capacitance	0·235 μF/phase star connection
Charging current at 50 c/s	1·406 amp
Equivalent current on l.v. side of transformer	107·2 amp

⊙ Intersection of magnetization and line-charging curves.

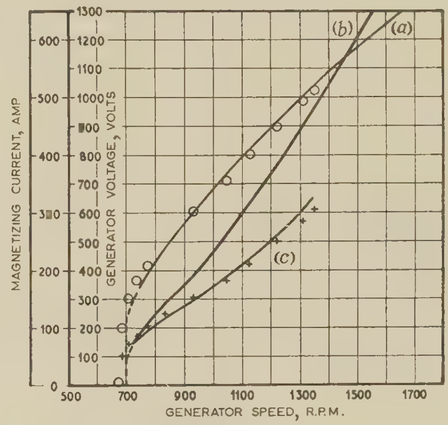

Fig. 5.—Quoich induction generator excitation tests. Calculated and test curves.

(a) Speed/voltage curve.
○ $\sqrt{3}V_{y\,phase}$ (test figures).
(b) Speed/current curve for transformer high-voltage side.
(c) Speed/current curve for current at generator only.
+ Average current for generator only (test figures).

former as the voltage builds up. Fig. 6(b) shows conditions with full-load throw-off while retaining connection to the 33 kV line. The rise of voltage can be clearly seen, and as the overspeed protection had been rendered inoperative, the overvoltage protection (set at 120%) shut the machine down. The maximum speed reached was 190% of normal and the maximum voltage 150%.

(3.1.4) Conclusions Drawn from Tests.

In actual operating conditions, the 33 kV line would have a number of distribution transformers connected to it. Opening the 33 kV switch with the generator delivering load would leave not only the 33 kV line connected but these transformers also. There would therefore be transformer magnetizing current and consumer's load, both of which would reduce somewhat the speed and voltage rise.

However, there are many cases where these effects would be quite small. It is to be noted that protection to prevent excessive speed and voltage is required, not only for the generator itself but for consumers' apparatus supplied from any transformers tapped off the 33 kV line.

(3.2) Magnetizing Current

(3.2.1) Switching in and Build-up of Magnetization.

When a generator is being brought on to load, it is run up by its turbine to approximately synchronous speed and the main switch is then closed, thus connecting the stator to the full system voltage. The stator then becomes magnetized, and opening up the water control to the turbine will cause the generator to deliver load.

Switching the stator on to the system causes a transient magnetizing current to flow, of several times the full-load current. However, the high current is short-lived, and thus its disturbing effect may not be very noticeable. Nevertheless, a current of such magnitude will cause quite a severe voltage drop,

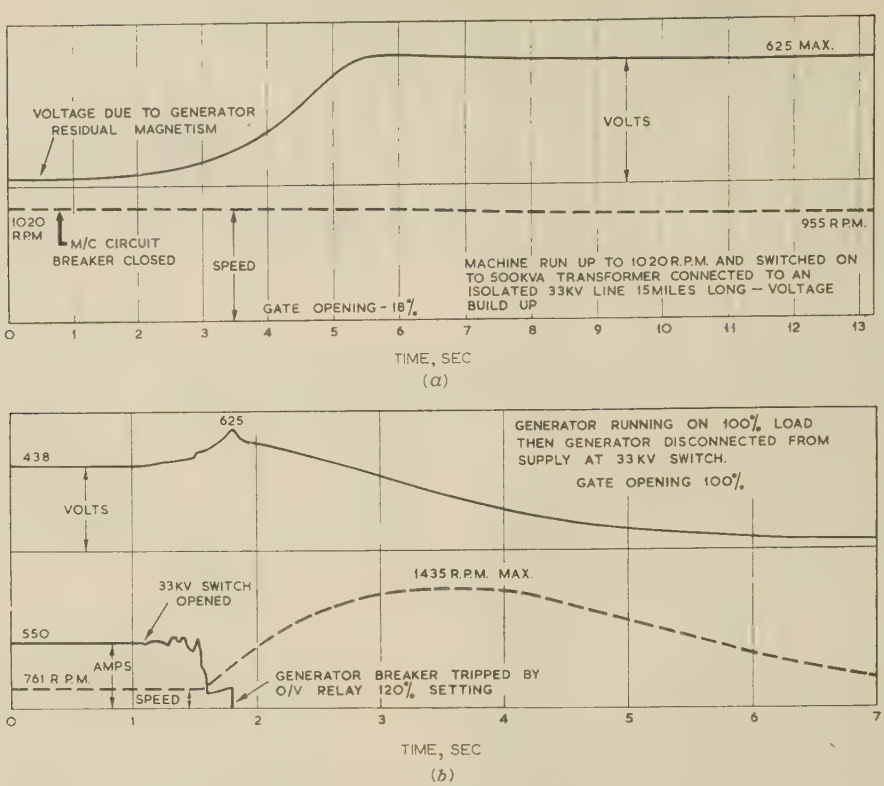

Fig. 6.—Oscillograms of Quoich induction generator excitation tests.

if the line or system connected to the generator is rated fairly closely to the generator output.

(3.2.2) **Tests of Switch-in Currents.**

Tests with an oscillograph have been carried out on a 2·4 MW induction generator at Mullardoch tunnel station to measure the switch-in currents, their duration and how they were affected by the speed being below, at or above synchronous. Fig. 7 shows the electrical connections and constants of the system to which the machine is connected.

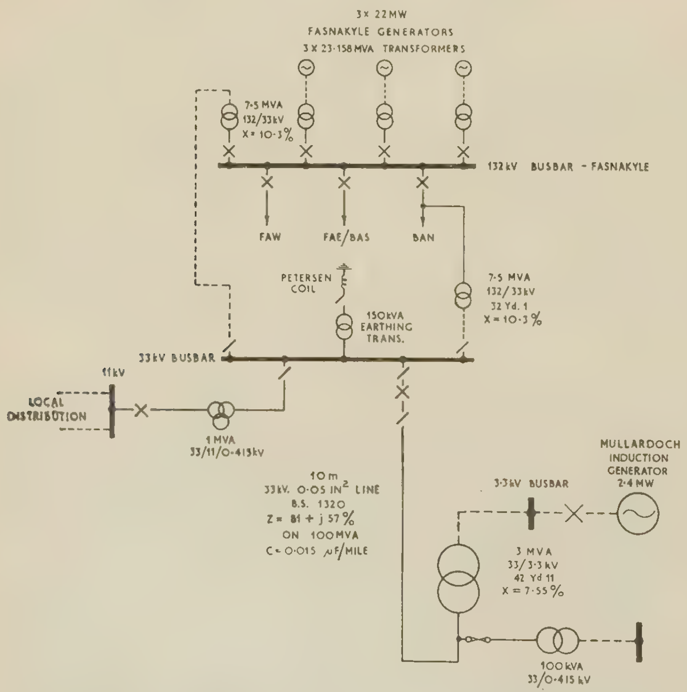

Fig. 7.—Connections of Mullardoch 2·4 MW generator.

Table 1

SUMMARY OF SWITCH-IN CURRENT TESTS

Speed	Peak current amp	Recovery time cycles	Minimum voltage kV (r.m.s.)
Synchronous (3 tests)	4060 4540 4800	20 19 24	1·85 1·81 2·03
Mean ..	4430	21	1·90
−5% (4 tests)	2820 3980 5070 4976	13 26 24 17	2·08 2·0 1·81 1·85
Mean ..	4210	20	1·94
+5% (3 tests)	5240 3150 3700	16 17 16	1·65 2·08 2·03
Mean ..	4000	16	1·92
+10% (3 tests)	4060 4440 5100	20 20 28	2·03 1·92 1·81
Mean ..	4530	23	1·92

Open-circuit voltage: 3·5 kV.
Running voltage: 3·4 kV (no load).

The following results from oscillograms are reproduced in Fig. 8 to illustrate the conditions which occur:

Switch in at 755 r.p.m. (approximately synchronous speed).
Switch in at 710 r.p.m. (6% low).
Switch in at 827 r.p.m. (9·5% high).

At the generating station there was a noticeable voltage dip in the lights, which lasted only a very short time. The size of

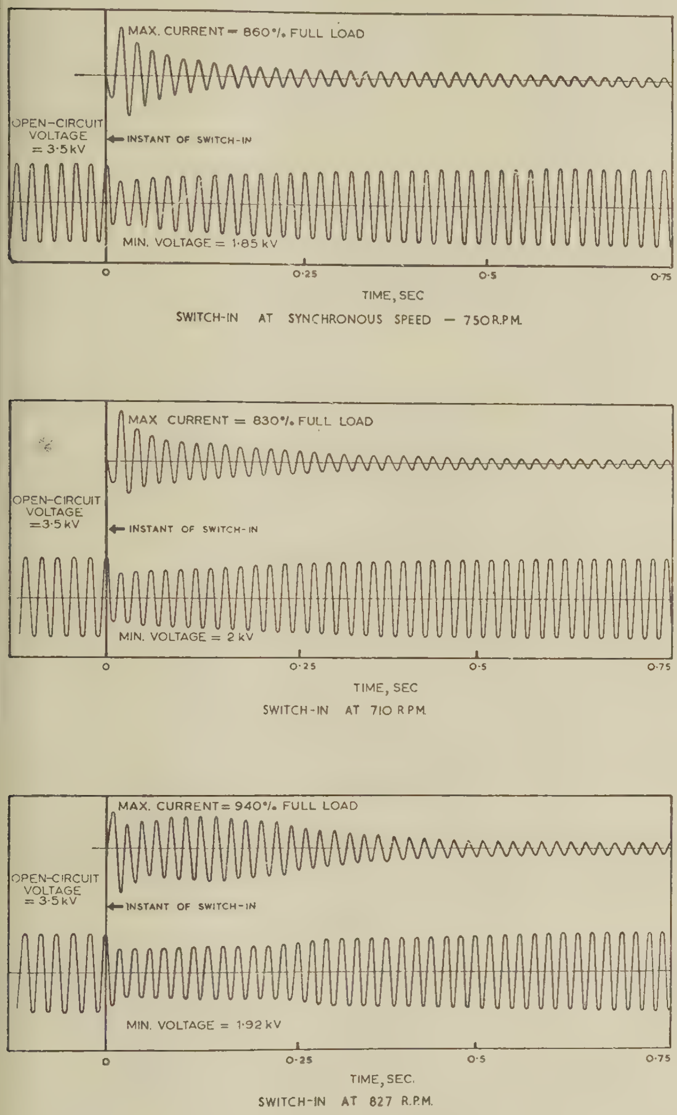

Fig. 8.—Mullardoch induction generator. Switch-in at various speeds.

the voltage drop measured had no apparent connection with machine speed when the switch was closed. At the main Fasnakyle generating station and 132 kV switching station no dip was noticeable in the lights, but a very slight change could be seen on the 11 kV recording voltmeter chart of about $\frac{1}{2}$%.

On switch-in the minimum voltage reached was about 55% of the open-circuit value at the switchgear busbars. No records of voltage at the 33 kV busbars at Fasnakyle were taken, but, from the size and length of the 33 kV overhead line, it would be expected that about two-thirds of the drop would take place in it. There does not seem to be any definite connection between the recovery time or the value of the peak current and the speed at which switch-in took place.

The maximum current at switch-in varies quite widely, but, again, the speed at switch-in seems to have little or no connection with this. The mean maximum current at switch-in, 4·3 kA (peak), is 25 times the no-load current and about 6·5 times the normal full-load current.

Machine speed at switch-in affects the shape of the envelope of the current peaks. When the machine is running at synchronous speed, the current at switch-in is mainly reactive, and the transient is that produced by the magnetic field being built up. But if the machine is not at synchronous speed, the current builds up the magnetic field and begins to die away. However, as soon as some field is established, power interchange begins, which sets up another transient. The combination of the two transients gives the fall and rise characteristic.

It does not seem that the time for the current to reach a steady value is much affected by the speed at which switch-in takes place.

(3.2.3) Conclusions Drawn from Tests.

From the results of the tests, it seems that errors in speed matching of −5 to +10% are not of very great importance and have very little influence on the transient effects on the system to which an induction generator is connected. This can be explained by considering the induction machine as equivalent to a transformer with a short-circuited secondary winding, the reactance of which is practically independent of the speed.

Once the magnetic field begins to be established, torque is produced in the rotor, if its speed is other than synchronous, and power is required to accelerate or decelerate the rotor, gearbox and turbine, which have considerable inertia. It would therefore be expected that the time for the initial disturbance to die out would be extended in some relation to the departure from synchronous speed, but this is not brought out in the test results. Any such effects are probably masked by the variations produced by the point on the wave at which switching takes place, and possibly, if the departure from synchronism were very large, the effect would be more obvious.

(4) REACTIVE LOAD IN NETWORK

The system in the north of Scotland has a high proportion of line-charging current, particularly from the 132 kV overhead-line system. The mileage of 132 kV line is large in relation to the load because there are few large centres of population and the distance between bulk supply points is fairly great. The water power is mainly in mountainous areas in the west, with little population and no load, so that generated output is transmitted considerable distances to load centres mainly in the east.

In addition to supplying the load in the north of Scotland, power is exported to the south of Scotland in the more highly loaded times of the day. Power is only exported at night if there is surplus generation from the hydro-electric stations, and then not at the same high rates as during the day. Therefore, there is a relatively high proportion of line-charging current at all times, but at night, when there is only a small amount of generating plant running, it becomes particularly difficult to keep the voltage of the 132 kV system down. Some 132 kV lines have to be switched out, and up to 50 MVA of plant run under-excited as synchronous reactors. All machines generating power at such times have to operate at the lowest leading power factor consistent with stability.

The addition of lagging magnetizing current by induction

generators is therefore more of a help than anything else, as the following figures for the 132 kV network show:

	Day-time		Night-time	
	Summer	Winter	Summer	Winter
Generator synchronous power, MW	500	870	55	85
132 kV circuit length, miles	1 500	1 593	1 270	1 312
132 kV line-charging power, MVA	126	137	110	113
System load lagging reactive power, MVA	110	190	12	18
132 kV transformer magnetizing power, MVA	32	35	30	30
Induction generator magnetizing power, MVA	11	12	4	6

(5) TRANSMISSION-LINE REGULATION

As explained in Section 4, there is no problem in supplying the magnetizing current for induction generators. In fact, the situation is rather the reverse. Since magnetizing power is supplied from synchronous generators on the main network, the active power from the induction generators is, in effect, transmitted at a leading power factor with a consequent reduction in voltage drop.

This can be illustrated by the 2·4 MW induction generator at Mullardoch tunnel. Fig. 9 shows a regulation chart for the

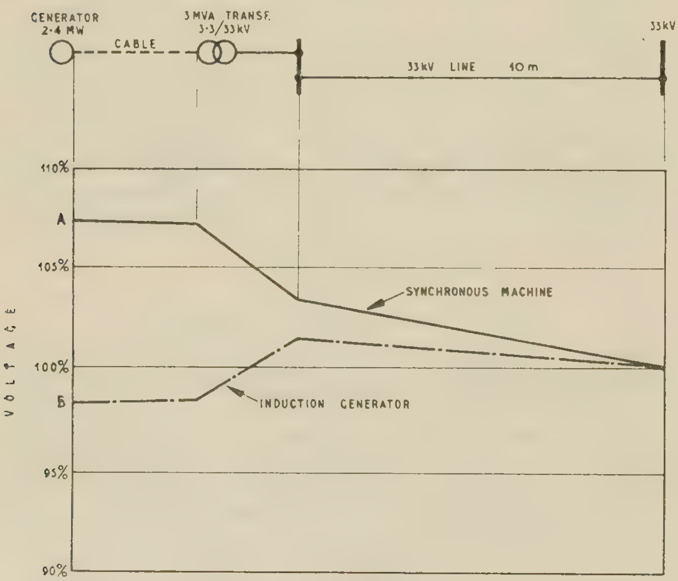

Fig. 9.—Voltage conditions with 2·4 MW induction generator (0·86 power factor leading) and alternative synchronous generator working at 0·9 power factor lagging.

Based on constant voltage at 33 kV busbars at Fasnakyle.

3·3 kV cable, 3 MVA step-up transformer and 12·5 miles of 33 kV overhead line connecting it to the 132/33 kV substation at Fasnakyle. If the generator had been a synchronous one running at 0·9 power factor, lagging the voltage drop from the machine terminals to the 33 kV busbars would be 7·2%, as shown at point A on the diagram. With the induction generator, which runs at a power factor of 0·86 leading, there is a voltage rise of $1\frac{1}{2}$%, as shown at point B. The copper losses on the transmission system are, however, increased by the induction generator to 79 kW in place of 71 kW—a rise of 11%.

(6) CONTROL AND PROTECTION

(6.1) Load Control

Induction generators are usually run at a steady load over a period of time, and so the arrangement for altering the turbine output can be simply the minimum required to vary the admission of water. Smaller machines can usually have hand-controlled systems, but larger machines require more power to operate the control through an oil-pressure servo-motor or a motor drive through gears. The following different kinds of output control system exist:

Hand-controlled setting of guide vanes.
Hand-controlled setting of intake gate (no guide vanes).
Motor drive through gearing for guide vanes.
Hand-controlled setting of needle valve.
Motor drive through gearing for needle valve.
Hand-controlled setting for guide vanes and runner blades (Kaplan machine).
Hand-controlled setting of guide vanes and hand-controlled oil-servo motor for runner blades (Kaplan machine).
Oil-servo operation of guide vanes and runner blades (Kaplan machine).

Motor or solenoid actuation of the pilot valve is used on larger turbines fitted with oil-servo-motor systems.

Speed-sensitive governor actuators are not used, since the machine load is steady, and switching in at speed *near* synchronous is all that is needed.

(6.2) Over-speed

Over-speed is required to shut the set down on loss of load or failure of normal regulation. While the generator is normally designed to withstand the maximum turbine over-speed, the over-speed device is set at some figure reasonably above the normal running speed. Inlet controls, normally handset, are sometimes closed by a hydraulic piston or sometimes by a belt drive from the main shaft, the drive being clutched in by the over-speed device; another arrangement uses a weight to close the guide vanes. Turbines fitted with guide-vane motor-drive or solenoid control of the servo pilot valve are easily shut down on operation of the over-speed device by the usual kind of electrical controls.

(6.3) Over-voltage

Over-voltage protection is fitted as standard, to guard against self-excitation which might arise, as described in Section 3.1.1. Switch operations on the electrical network which could give rise to over-voltage are possible in most cases, although in some cases they are improbable. The cost of adding an over-voltage relay to the main circuit-breaker is quite small. The relay need only open the circuit-breaker, but, if convenient, it is also usually arranged to shut down the turbine.

(6.4) Low Voltage

Low-voltage protection is also a standard fitting (it must, however, have a time lag and not be sensitive to short-time voltage dips). Its principal use is to open the main circuit-breaker if the network supply fails. In this event, the generator will lose its load, over-speed and be shut down by over-speed or over-voltage protection, which, in some cases, cannot conveniently be made to trip the switch. Restoration of the supply to the electrical network will normally be from a source remote from the induction generator, and it is necessary to disconnect it from the supply line before restoration, otherwise it might be

switched in when at rest. In the unlikely event of the protection failing to shut the set down, restoration of the supply with the machine at over-speed might not restore it to normal load speed. Under conditions of reduced voltage, stable operation at low power output and high speed is possible. The low-voltage protection ensures that this cannot happen.

(6.5) Other Protection

Other protective devices, electrical and mechanical, follow the lines usual on water turbine-alternator installations. Electrical winding and temperature protection and devices such as bearing thermometers, oil- and water-flow devices are fitted as required. Many induction-generator installations are small and are usually less complicated.

(7) STARTING AND SYNCHRONIZING

(7.1) General Requirements

Since machines have no speed governors, the water admission control is operated to give a speed reasonably near synchronous before the main electrical switch is closed. Small turbines and those of the Francis type are quite simply controlled; control at starting has been quite satisfactory, too, with the comparatively large 2·4 MW vertical Francis turbine at Mullardoch generating station. Kaplan machines present somewhat more difficult conditions, because the guide-vane opening required to overcome initial bearing friction is often greater than that for normal speed at no load. This means that the guide vanes may have to be reclosed once 'breakaway' has occurred, and, with over-speed device trip settings of about 130% usually adopted, there may be little time to control the speed, since acceleration is fairly rapid.

Experience so far with a 2·4 MW vertical-shaft Kaplan-type turbine-driven generator has shown it to be reasonably easily controlled during starting under manual control. The runner blades have to be fully open at the start, and once the set is on load, they are allowed to take up a position for optimum efficiency, related to guide-vane opening by combinator gear.

(7.2) Automatic Control of Machines Driven by Kaplan Turbines

When Kaplan plants are remotely controlled, there is rather more difficulty about blade-position control. At present, there are two remotely controlled stations each with a 4 MW set driven by a vertical Kaplan turbine. They are arranged for local-automatic as well as remote control of starting and stopping. When on remote control the start sequence once begun will be automatic right up to closure of the main switch. No special or new problems arise when stopping, but, for the starting sequence, automatic control of the guide vane and runner-blade openings are required. A large gate-opening is needed to start the machine turning, followed by a closure to prevent too rapid a rise in speed. This needs to be followed again by opening of the guide vanes to the no-load position. The control of the closing and opening to the no-load position is carried out by a voltage-sensitive relay connected to a permanent-magnet generator on the main machine shaft. The output is arranged to operate the relay even when the machine shaft is turning at low speed. When the guide vanes have finally reopened to the no-load setting, synchronizing takes place under control of a speed-matching relay, with its incoming side fed from a permanent-magnet generator. Under local manual control the feature whereby the guide vanes are automatically closed once the turbine has started to rotate is retained, and synchronizing is carried out with the aid of a tachometer and stroboscope.

(8) DRYING OUT OF WINDINGS

Synchronous machines are usually dried out on 3-phase short-circuit at about full-load stator current, produced by control of the rotor excitation. This method is not applicable to induction generators, since they have no exciter.

For medium-voltage or 3·3 kV windings electric heaters suitably placed in the stator frame for some time before commissioning have made the windings sufficiently dry for energizing.

For 11 kV windings, such indirect heating and drying is not enough and some means of circulating heating current at low voltage in the stator is needed. A motor-generator set may be used to pass direct current through the 3-phase windings connected in series, and by this means, drying out can be done with similar effects to short-circuit current heating.

(9) CHECKING OF PHASE ROTATION

(9.1) Conditions of the Problem

The phase rotation of synchronous machines is measured against that of the electrical system to which they are to be connected. This requires excitation to normal voltage and voltage measurements direct or through voltage transformers, depending on the machine voltage. In an induction generator no voltage exists on the windings until the stator is switched on to the network, and thus different methods are required.

(9.2) Use of Synchronous Generator

If a synchronous generator can be made available for connection to the induction generator, free of all connections to the main electrical network, its excitation is gradually increased and measurements of the stator currents are made while both machines run up together. If the phase rotation of the induction generator is correct, the current at any voltage will not exceed the normal values. If the rotation is wrong, higher currents will begin to appear before the full voltage is reached.

(9.3) Two-Phase Connection

The induction generator, running at normal speed, is connected to the electrical network on two phases only. The remaining phase of the machine is connected to the third network phase through a voltmeter or voltage transformer and voltmeter, depending on the voltage. With correct phase rotation a small voltage will be measured, but with wrong rotation a voltage about twice the normal will exist.

This method is useful if no separate synchronous machine is available for test 1, but it has the disadvantage that connection of the stator to two phases only of the system will cause unbalanced forces in the machine winding. These will not be serious for machines having a large number of pole pairs, but the method is unsuitable for higher-speed machines.

(9.4) D.C. Battery Method

The following method is always possible. It requires no separate synchronous machine and no connections to the electrical system (see Fig. 10):

A battery of about 12 volts d.c. is connected to two stator phases while a d.c. voltmeter is connected between the remaining phase and one of the phase-battery connections. The battery is switched on with the machine at rest and the direct current in phases A and C will produce north and south poles at the points n_a, s_a, n_b and s_b in the stator and north and south poles in the rotor. If the rotor now rotates clockwise, the right-hand coil of phase B will be swept by a south pole in the rotor and the left-hand side by a north pole. For correct phase

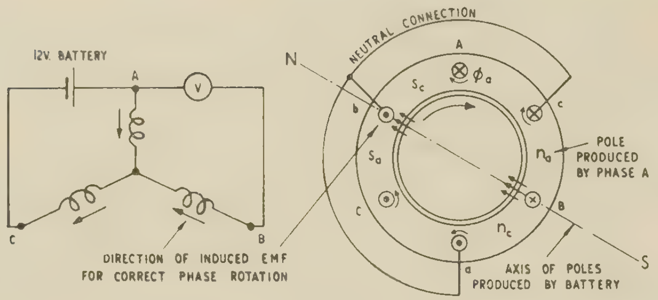

Fig. 10.—Phase-rotation measurement.

rotation the voltage induced in phase B should be as shown in Fig. 10. Before rotation commences the voltmeter will give a reading because of the voltage drop in phase A caused by the battery current. With correct phase rotation, this voltage will fall as the machine commences to rotate. Wrong phase rotation will result in an increase in the voltmeter reading.

(10) DESCRIPTION OF TYPICAL SCHEMES

(10.1) Pitlochry Fish Pass Station, Compensation Water Set (Figs. 11 and 12)

At the main Pitlochry power station, where there are two 7·5 MW Kaplan sets under a net head of 46 ft, there is a pool type of fish pass where a discharge of water is required at the entrance to attract salmon in. The required flow of 18·5 cusec at the station head made it economic to install a 50 kW generating set. This is a horizontal Francis machine with a 415-volt induction generator. The average annual output of $0{\cdot}4 \times 10^6$ kWh is delivered to the power-station common services board, which is connected through a transformer to the local 11 kV network.

Guide-vane control is by handwheel, while synchronizing is carried out with the aid of a tachometer and neon-lamp stroboscope. In order to shut the set down if, for instance, the generated output is lost, a weight-operated automatic device is fitted, actuated by an over-speed relay. The device consists of a vertical dashpot with a weight connected by wire rope to the turbine guide-vane handgear, the weight being supported by a quick-release ratchet mechanism. The mechanism is released by the over-speed relay in which centrifugal force on an unbalanced pin fitted to the turbine shaft causes an outward movement. The switchgear is provided with direct-acting over-current and under-voltage release. The set is therefore protected against the more likely faults and is arranged to be as simple as possible.

(10.2) Mullardoch Tunnel Generating Station (Figs. 13 and 14)

The Mullardoch generating station forms part of the Glen Affric scheme and is situated at the intake end of a tunnel which connects Loch Mullardoch and Loch Benevean. The main station is of 66 MW at Fasnakyle, and most of its storage is contained in Loch Mullardoch, the top water level of which is a maximum of 82 ft above that of Loch Benevean, which feeds the main station. The Mullardoch station was constructed to make use of this difference in level.

A horizontal 2·4 MW 755 r.p.m. induction generator is driven by a vertical Francis turbine through a right-angle speed-raising gear unit. After standby lubricating oil pressure is established,

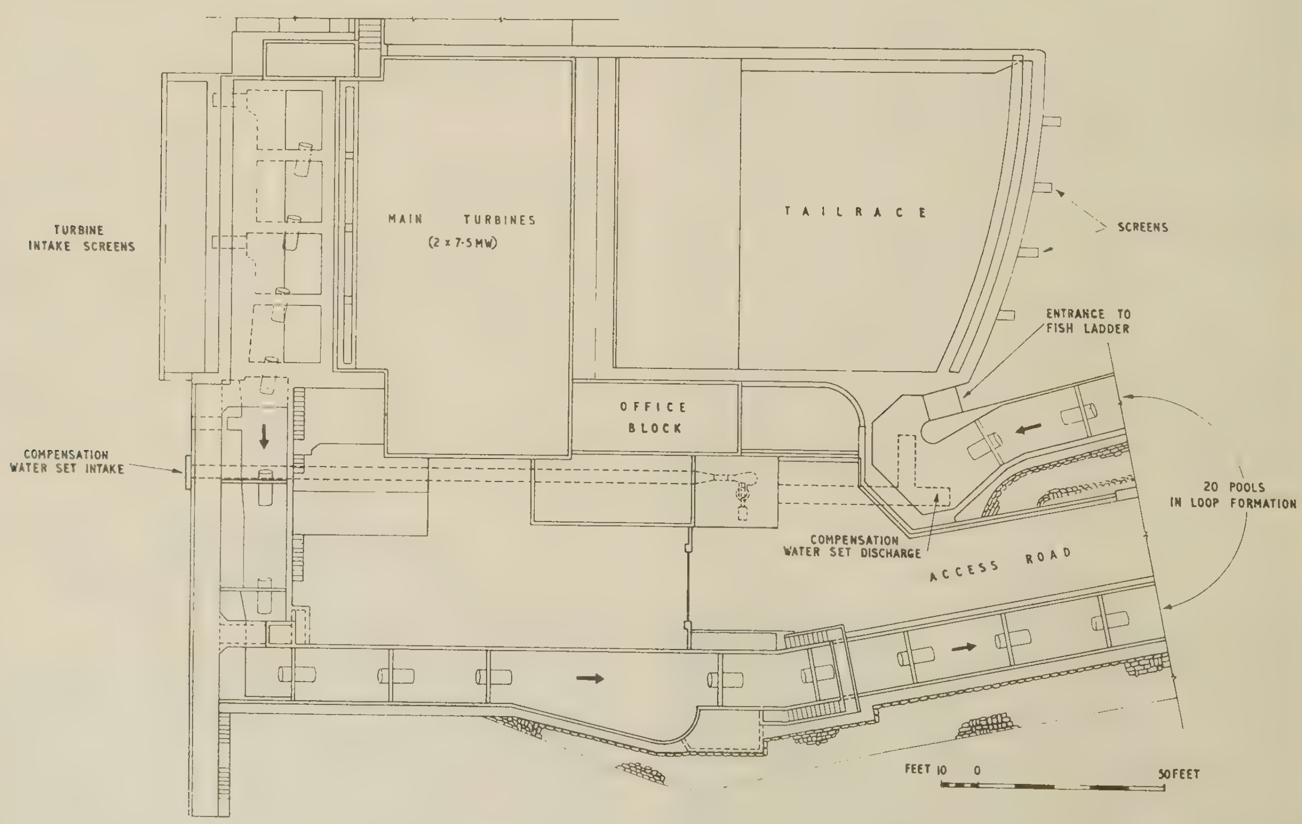

Fig. 11.—Plan view of Pitlochry power station.

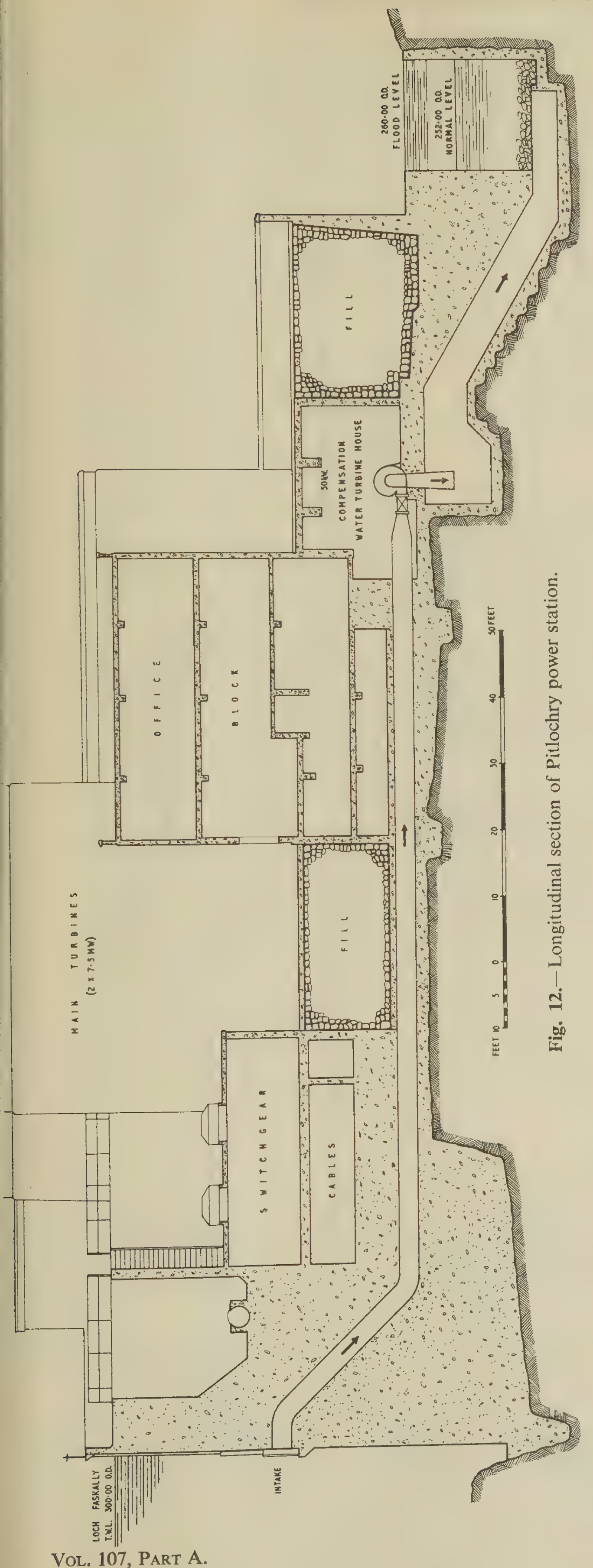

Fig. 12.—Longitudinal section of Pitlochry power station.

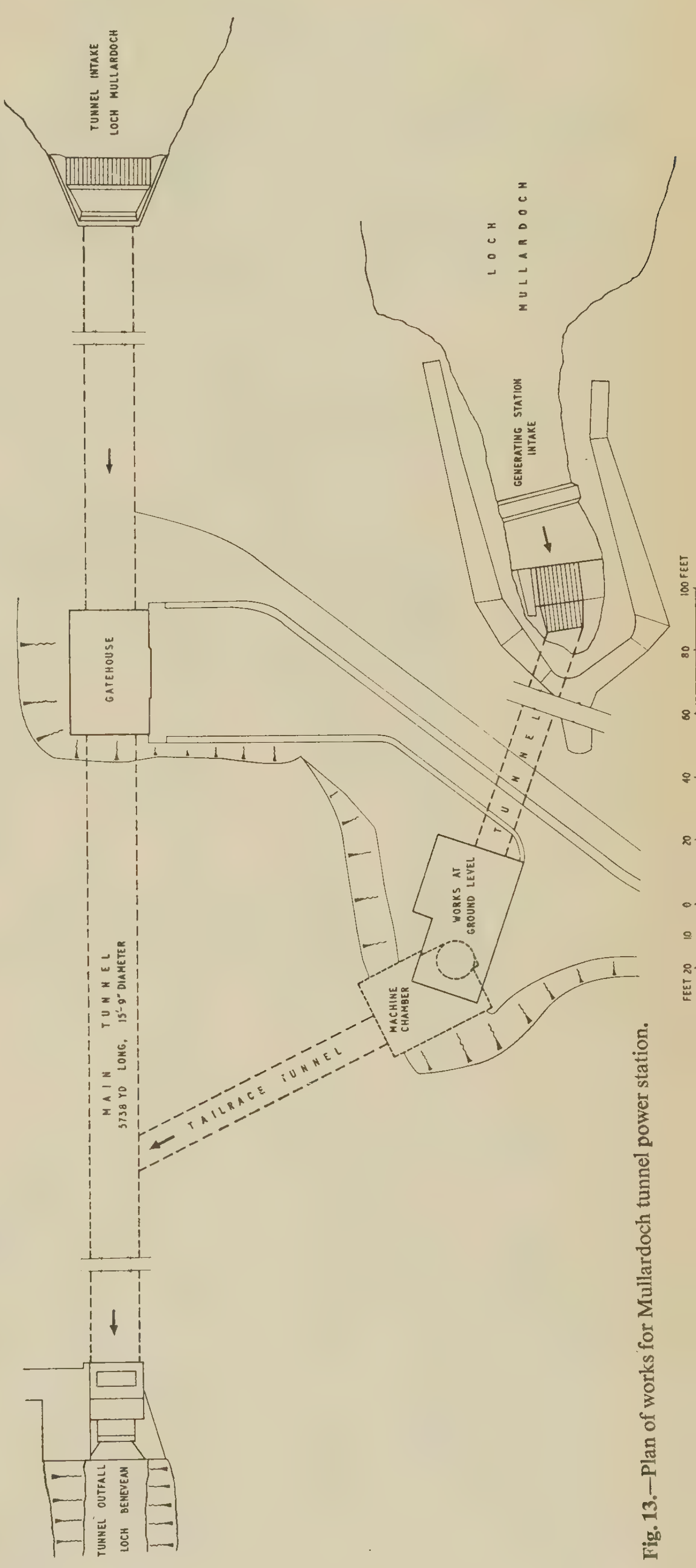

Fig. 13.—Plan of works for Mullardoch tunnel power station.

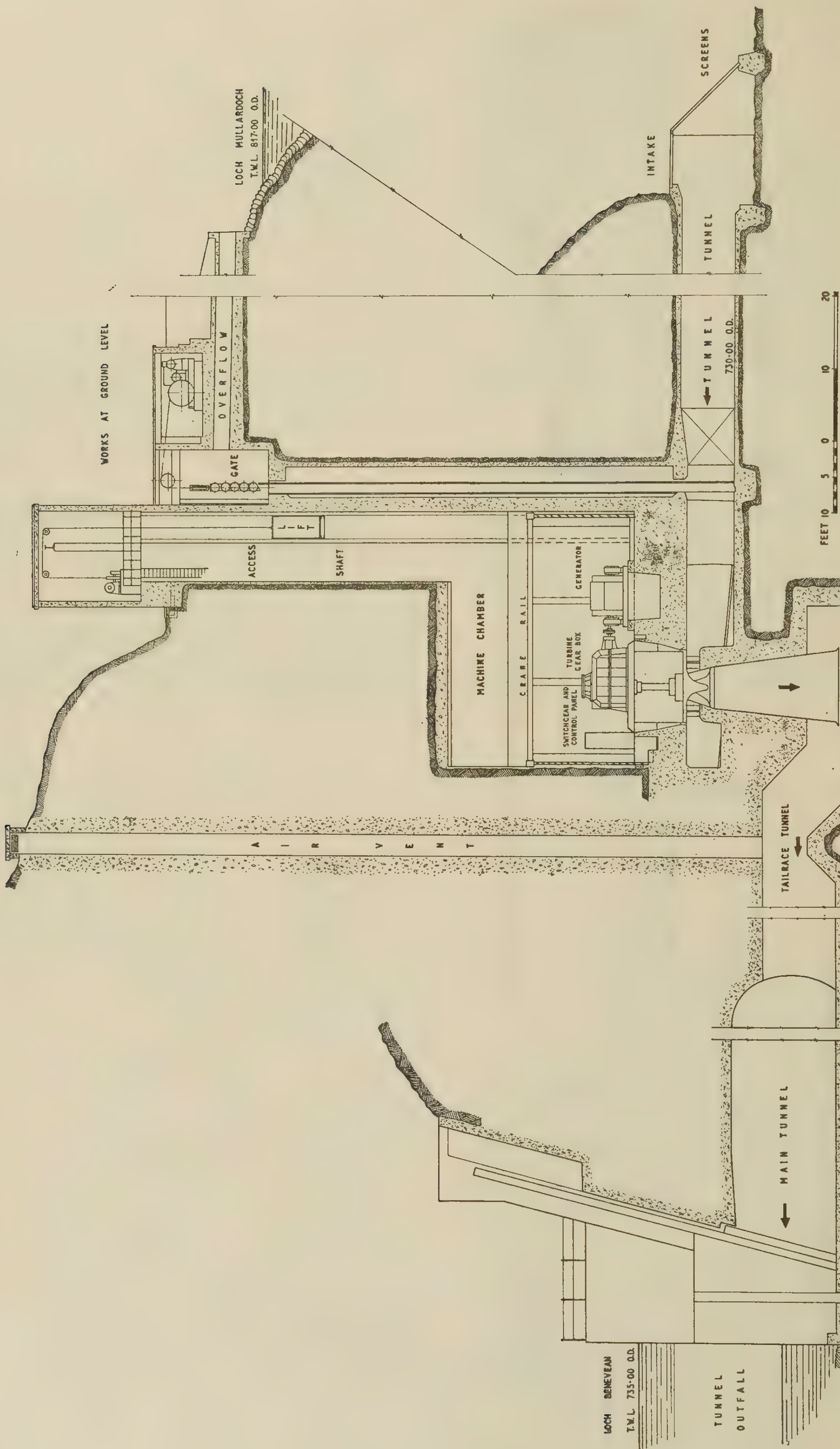

Fig. 14.—Longitudinal section of Mullardoch tunnel power station.

the turbine guide vanes are operated by a 1·5 h.p. d.c. motor and the standby oil pump is shut down automatically when the gear-driven main oil pump builds up sufficient pressure. Guide-vane position is adjusted by hand control, and for speed-matching there is a stroboscope driven by a supply obtained from a permanent-magnet generator on the main shaft and illuminated by a neon lamp connected to a voltage transformer on the main-switchgear busbars.

The protective devices provided operate a shut-down relay, and are as follows:

Generator-stator electrical faults.
Overload.
Over-speed.
High stator air temperature.
High bearing temperature.
High gearbox lubricating oil temperature.
Low lubricating oil pressure.
Under-voltage (a relay provided with a time lag).

The shut-down relay in turn opens the main circuit-breaker and closes the guide vanes. Should the guide vanes not close within 6 min of the shut-down relay operating, the intake gate is automatically closed.

The output of the generator is switched at 3·3 kV and stepped up to 33 kV by a 3 MVA transformer, and taken by 12½ miles of 0·05 in^2 overhead line to Fasnakyle switching station, which is connected to the 132 kV Grid system.

Table 2

INDUCTION GENERATORS

Installation	Duty	Turbine	Maximum gross head	Speed	Voltage	Average annual output	
			ft	r.p.m.	volts	kW	kWh × 10^6
			In Operation				
Pitlochry	C.W.	Horizontal Francis	45	762	415	50	0·4
Clunie	C.W.	Horizontal Francis	58	515	415	175	0·8
Mullardoch	R.R.	Vertical Francis	92	T. 208 G. 755	3 300	2 400	8
Torr Achilty	C.W.	Horizontal Francis	45	610	415	100	0·5
Luichart	C.W.	Horizontal Francis	57	765	415	85	0·5
Achanalt	L.G.	Vertical Kaplan	49	336	11 000	2 400	8
Meig Dam	C.W.	Horizontal Francis	50	765	415	76	0·4
Invergarry	C.W.	Vertical Francis	33	T. 230 G. 760	415	285	0·9
Invergarry	C.W.	Vertical operating flume Francis	33	760	415	30	
Quoich	C.W.	Horizontal Francis	123·5	762	415	350	2·2
Cluanie	C.W.	Horizontal Francis	114	507	415	300	1·8
Kilmelfort	C.W.	Horizontal impulse	355	1 020	415	82	0·4
Tralaig	C.W.	Propeller	28·5	510	415	83	
Sron Mor	L.G. and pumping	Horizontal Francis with pump	170	303	11 000	5 000	6
Lochay Fish Pass	C.W.	Vertical Francis	54	1 020	415	54	0·3
Dalchonzie	L.G.	Vertical Kaplan	107	336	3 300	4 000	18
Lubreoch	L.G.	Vertical Kaplan	98	336	3 300	4 000	13
Glenmoriston	C.W.	Vertical propeller	47	1 020	415	160	0·7
Vaich Tunnel	R.R.	Vertical Francis	70	T. 113·5 G. 508	415	320	1·1
Stronuich	C.W.	Vertical propeller	36	610	415	210	1·0
					Totals	20 160	64·0
			Under Construction				
Errochty	C.W.	Horizontal impulse	298	434	415	550	1·75
Loyne Tunnel	R.R.	Vertical Kaplan	85	434	415	550	2·2
Shin. Div. Weir	C.W.	Horizontal Axial flow propeller	19·5	434	415	100	0·4
Lochay	C.W.	Horizontal 2 jet Pelton	588	433	3 300	2 000	
Orrin	C.W.	Horizontal Francis	138	760	415	200	0·9
Orrin	C.W.	Horizontal Francis	138	1 525	415	56	
Duchally	A.D.	Horizontal Francis	84	760	415	325	
Duchally	A.D.	Horizontal Francis	209	610	415	125	5
Lednock	L.G.	Horizontal Francis	310	504	3 300	3 000	
					Totals	6 906	10·25

C.W. Compensation water.
R.R. Reservoir regulation.
L.G. Load generation.
A.D. Aqueduct diversion.
T. Turbine
G. Generator

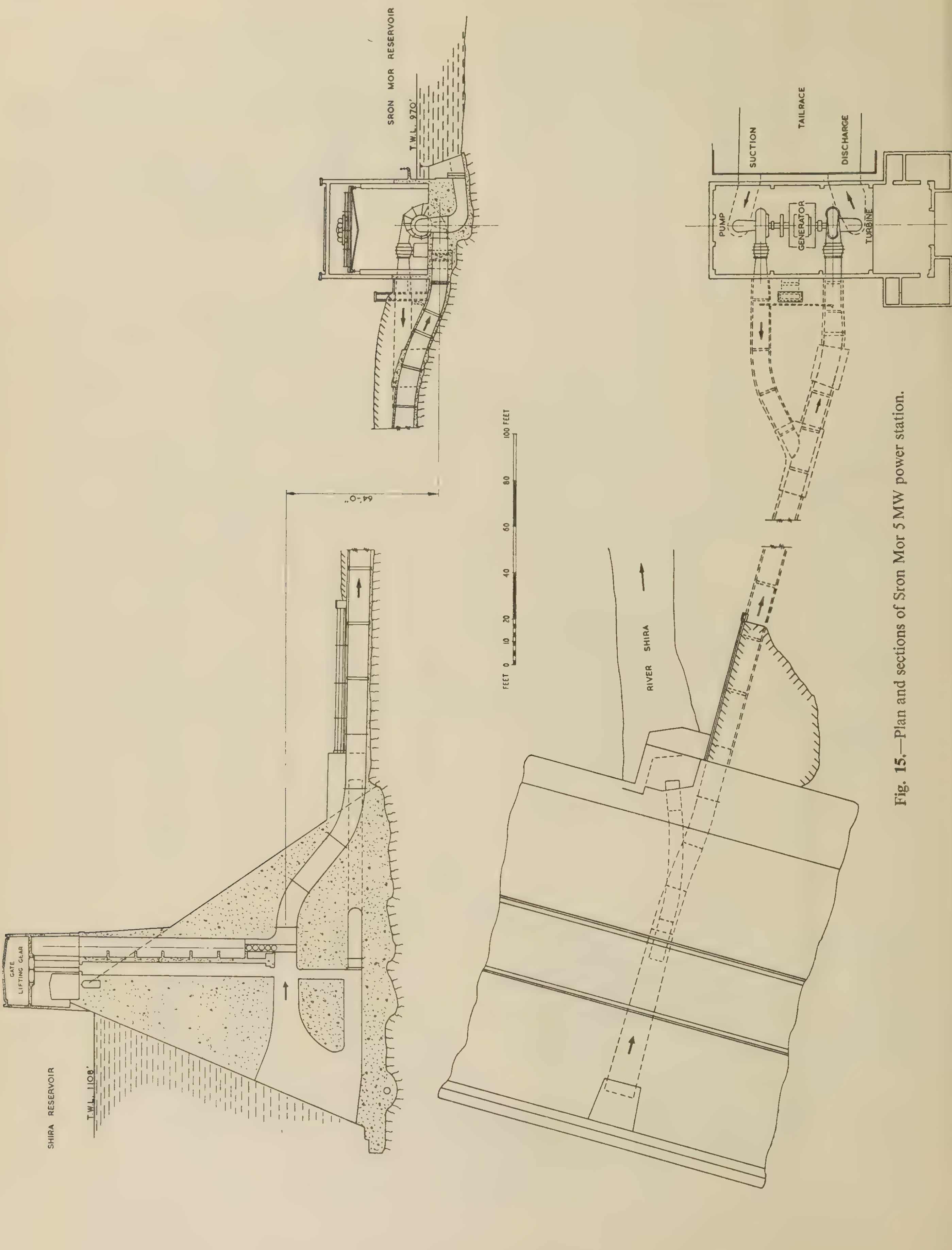

Fig. 15.—Plan and sections of Sron Mor 5 MW power station.

The station is normally unattended, but an attendant lives nearby and is responsible for general maintenance in the station and Mullardoch headworks. An alarm is taken to the attendant's house and also to the continuously staffed Fasnakyle power station 13 miles away. Telephone communication to Fasnakyle is also provided.

(10.3) Sron Mor Generating/Pumping Plant (Fig. 15)

The Sron Mor station is part of the Glen Shira scheme in Argyll and is situated between two reservoirs formed by dams at the head of the Glen. The upper reservoir has a capacity of $792 \times 10^6\,ft^3$ and a top water level of 1 100 ft with a catchment area of 13·36 square miles. The lower reservoir has a capacity of $57 \times 10^6\,ft^3$ and top water level of 970 ft, with a catchment area of 8·12 square miles. It will be seen that the main storage of the scheme is provided by the upper reservoir. Water is led from this reservoir through a 5 MW set at Sron Mor into the lower reservoir; alternatively the set may be used to pump water from the lower to the higher reservoir. The main tunnel leads from the lower reservoir to the Clachan power station, where a single 40 MW set is installed at about 10 ft above sea-level.

The set installed in Sron Mor station is a horizontal-shaft combined unit consisting of a Francis turbine, an induction generator, a flywheel and a single-entry scroll-cased centrifugal pump coupled solidly to the flywheel. Provision is made for uncoupling the flywheel and pump when generating during dry periods. During pumping, the turbine is dewatered with compressed air, and likewise during generating periods the pump runs in air. A cooling-water supply to the neck rings and glands is provided. The purpose of the flywheel is to avoid dangerous pipeline pressures in the event of power failure when pumping.

The induction generator is rated at 5 MW 0·89 power factor 11 kV, and at full load it rotates at a super-synchronous speed of 303 r.p.m. Machine ventilation is open-circuit, the air being drawn axially along the rotor and discharged centrifugally into the stator windings and core, and through an opening at the top of the casing into the station. The pedestal bearings are oil-pressure fed, assisted by oil rings, from a pump belt-driven from the main shaft. A d.c. motor-driven pump is provided for standby use. An over-speed device and tachogenerator are mounted above and are gear-driven from the main shaft. Machine starting as either a turbine or a pump is semi-automatic, i.e. by operating a button the set is brought to a condition ready for opening the turbine gates. Gate opening is under manual control, as also is the closing of the 33 kV switch. When starting as a pump, the pump is automatically primed by an ejector before the 'ready to start' indication is given, otherwise the pump is first dewatered by compressed air. In either case the turbine drives the set up to synchronous speed.

The induction generator-motor is directly connected to a 6 MVA 11/33 kV transformer and switched at 33 kV. This substation 'tees' into the 33 kV line which runs between Clachan and Oban. The section between Sron Mor and Clachan is approximately 6 miles of $0{\cdot}075\,in^2$ section, and at Clachan there is a 7·5 MVA transformer on the 11 kV busbars which are connected to the 132 kV Grid system through a 50 MVA transformer, which also handles the output of the main 40 MW machine.

(11) ECONOMICS OF SCHEMES

The use of induction generators produces savings in capital cost arising from omission of governor actuator, automatic voltage regulator, as well as the control panels and wiring associated with them. Less building space is needed without these items of equipment. Without synchronous governing, savings can in many cases be made in conduits and pipelines because of reduced pressure-change requirements. Control and operation are much simplified, and hence staff attendance and operational costs are reduced.

Apart from the savings brought about by using induction generators, low total generation costs are achieved from compensation water sets and inter-reservoir installations. The main reason is the low cost of the additional constructional works, which have to be added to that of the main schemes. For example, small sets discharging compensation water from a main dam require only a small building extension for the generating plant; the intakes and discharges are simple and the pipelines are short. In the case of inter-reservoir installations, the main tunnel and outfall works are needed in any case and the extra works required for diverting water through the station and for housing the generating plant are relatively small.

The costs of electricity produced at such plants are usually between one-third and one-half of that at the main stations. Usually the generators are running or can be run at peak-load times, so that the installed power is available and the output has full economic value.

Although the amount of electricity produced by plants of the kinds which have been described is fairly small, the cheap costs of production make the works well worth while. Table 2 lists the main features of induction-generator installations in operation or under construction.

(12) ACKNOWLEDGMENTS

The author wishes to thank the North of Scotland Hydro-Electric Board for permission to publish the information about the Board's plants and to carry out the tests described; several colleagues for help with the paper and tests; Messrs. Bruce Peebles and Co., Ltd., for providing test equipment and helping with the tests; and Prof. E. O. Taylor for advice on vector presentation.

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(14) APPENDIX

DETAILED THEORETICAL CONSIDERATIONS

(14.1) Equivalent Quantities

On the assumption of a common flux, the stator and rotor induced voltages are given by

$$V_s = C_S \Phi_m f \quad . \quad . \quad . \quad . \quad . \quad (1)$$

$$V_R = C_r \Phi_m S f \quad . \quad . \quad . \quad . \quad . \quad (2)$$

where C_s and C_r are constants for stator and rotor windings,

respectively, Sf is the frequency of the currents in the rotor, running slightly above or below synchronous speed.

$$\frac{V_s}{V_R} = \frac{C_s}{C_r}\frac{f}{S_f} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (3)$$

$$V_R = SV_s\frac{C_r}{C_s} \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

The equivalent rotor voltage referred to the stator, V_r, is therefore SV_s. Similarly other rotor quantities are translated to stator equivalents.

(14.2) Equivalent Circuit

With the machine regarded as a transformer, a simplified diagram is as shown in Fig. 16, where the rotor quantities are

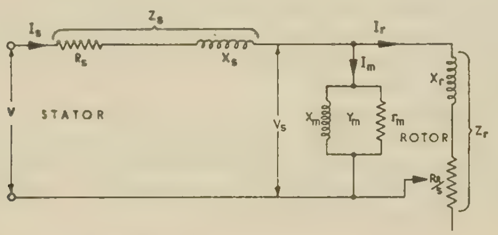

Fig. 16.—Equivalent circuit.

referred to the stator. The machine voltage and current can be analysed from this diagram as follows:

$$Z_r = \frac{R_R}{S} + jX_r$$

$$I_s = I_r + I_m \text{ (vectorially)}$$

$$I_s = V_s\left(\frac{I}{Z_r} + Y_m\right) \quad . \quad . \quad . \quad (5)$$

$$V = V_s + I_sZ_s$$

$$V = V_s\left(1 + \frac{Z_s}{Z_r} + Z_sY_m\right)$$

$1 + Z_sY_m$ is slightly greater than unity. Let us call it C.

Then $$V_s = V\frac{Z_r}{Z_s + CZ_r}$$

$$V_s = V\frac{\frac{R_R}{S} + jX_r}{R_s + jX_s + C\left(\frac{R_R}{S} + jX_r\right)} \quad . \quad . \quad . \quad . \quad (6)$$

$$I_r = \frac{V_s}{Z_r} = \frac{V}{R_s + jX_s + C\left(\frac{R_R}{S} + jX_r\right)} \quad . \quad . \quad . \quad (7)$$

From eqns. (5), (6) and (7), the stator current, induced stator voltage and rotor current can be calculated. The slip S must be taken as negative for a generator. Fig. 17 shows the way in which these quantities vary for different values of slip, both for motoring and generating.

(14.3) Torque and Power

The power input, P_R, to the rotor (motoring) is $I_r^2R_R/S$.

The rotor heat loss is SP_R and the mechanical power is $(1 - S)P_R$.

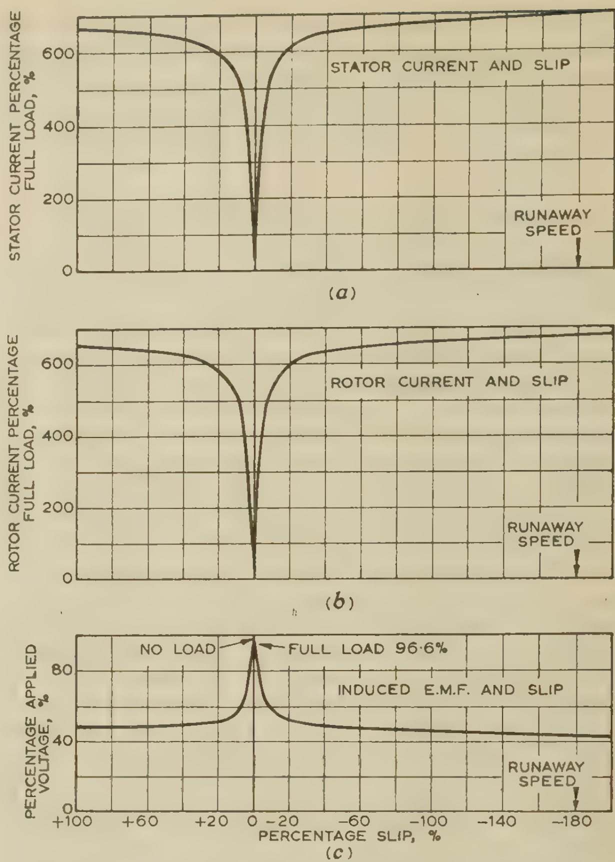

Fig. 17.—Mullardoch power station induction generator variation of (a) stator current, (b) rotor current and (c) induced e.m.f. with slip.

Using the scalar value of I_r from eqn. (7),

$$P_R = \frac{V^2\frac{R_R}{S}}{\left(R_s + \frac{CR_R}{S}\right)^2 + (X_s + CX_r)^2}$$

Fig. 18 shows the variation of torque and power with slip. The maximum shaft power and torque when generating is greater

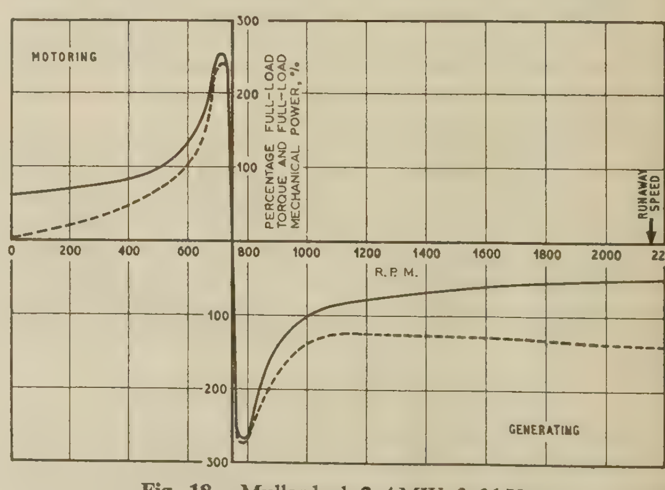

Fig. 18.—Mullardoch 2·4 MW, 3·3 kV.
755 r.p.m. induction generator speed/torque curve.
Full load torque = 23 200 lb-ft.
——— Torque.
- - - - Mechanical power.

than when motoring, as explained in Section 14.5 where the circle diagram is described.

(14.4) Efficiency

The efficiency for a generator is

$$\frac{\text{Power input} - \text{Losses}}{\text{Power input}}$$

The electrical losses are

Stator heating $I_s^2 R_S$

Rotor heating $I_r^2 \frac{R_R}{S}$

Magnetizing power $I_m^2 R_m$

and mechanical losses of friction plus windage.

(14.5) Circle Diagrams

Circle diagrams (based on a simpler equivalent circuit having Y_m transferred to the input terminals) bring out clearly the significance of various factors and show the difference between motor and generator performance.

(14.5.1) Motor [Fig. 19(*a*)].

V_s, I_s, I_{nl} and I_m are relatively in the same positions as in the vector diagram of Fig. 1(*a*). The diameter of the circle is $V/(X_s + X_r)$, i.e. the maximum current when the algebraic sum of R_s and R_R/S is zero. AP_m represents the magnetizing power loss and P_mP_{nl} the additional no-load losses of friction and windage. It is convenient to consider the friction and windage loss as constant. Windage is obviously not constant with varying speed and at short-circuit, i.e. locked rotor, it is, in fact, zero. However, while it decreases with decreasing speed, the rotor core loss will be increasing, because the rotor frequency is increasing. Thus over the complete range of operation P_mP_{nl} and AP_{nl} can be considered approximately constant.

P_{SC} represents the short-circuit or locked-rotor position. There is no shaft output, and so $P_{SC}C$ represents the rotor copper loss and CK the stator copper loss. The line $P_{nl}P_{SC}$ then enables these losses to be obtained at any other point.

At any operating point, P_M, TQ represents the no-load losses of magnetization, friction and windage, QS the stator copper loss, SR the rotor copper loss, leaving RP_M as the shaft output. SP_M represents the shaft power (shaft output plus rotor copper loss). The efficiency is RP_M/TP_M and the slip (rotor copper loss/power across air-gap) is SR/SP_M.

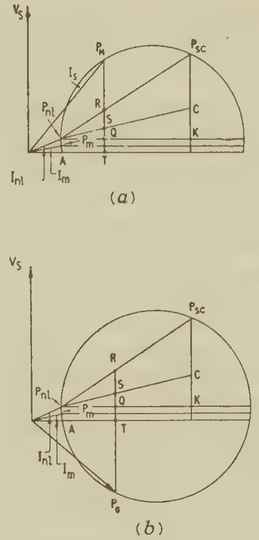

Fig. 19.—Circle diagram.
(*a*) Motor.
(*b*) Generator.

(14.5.2) Generator [Fig. 19(*b*)].

The diagram can be redrawn [Fig .19(*b*)] for a point where the slip is negative, and the corresponding characteristics can be derived for the same machine acting as an induction generator. The operating point is now P_G. The electrical power output is represented by P_GT. The no-load losses TQ, the stator copper loss QS, and the rotor copper loss SR, must be added to the electrical power to give the shaft power input P_GR. The efficiency is now P_GT/P_GR. It can be seen that the shaft power (or torque) now has to provide both the load and the no-load losses as well as the electrical output and is thus greater than in the motor case.

DISCUSSION BEFORE THE SUPPLY SECTION, 13TH APRIL, 1960

Prof. M. G. Say: The author gives some interesting details of the practical application, in conditions by no means easy, of the induction machine as a generator. The most important operating characteristic is the necessity for magnetizing power and the means by which it may be provided. Because there is no separate exciting circuit, such as is found in the synchronous machine, the magnetizing power must enter the machine at the stator terminals. An auxiliary generator to provide magnetization at slip frequency to rotor slip-rings would be technically feasible and would free the induction generator from its rather unyielding characteristics, but simplicity and economy would be lost.

I do not favour the change of aspect represented by (*a*) and (*b*) in Fig. 1. A consistent approach in which the 'external' viewpoint is held, regardless of whether the machine is generating or motoring, is to be preferred: the terminal voltage is always taken as 'applied', so that a current having a component in phase with it implies power input to the machine as a motor. For generator action there must be a current component in anti-phase. Any load across the machine remains a load, so that the magnetizing shunt circuit continues under all conditions to demand lagging reactive current.

Fig. 16 could be made more informative. With the usual transformations the rotor circuit incorporates a resistance R_r/s which varies with slip. It can be divided into the actual rotor resistance R_r (responsible for the I^2R loss) and the remainder, given by $R_r(1 - s)/s$. For generator action the latter term is negative, so that, when a current flows in it, a current-dependent driving e.m.f. is developed. The machine then operates in a more obvious way as a source e.m.f. connected through an impedance network to the terminals. The output current will be dependent upon the voltage applied to the terminals of the machine as well as upon the slip, because the e.m.f. is a function of the rotor current. The magnetizing circuit is a permanent load across the rotor, which cannot provide for it for the reason just given. But a capacitor connected across the output terminals can be resonated with the magnetizing circuit to interchange reactive power with it.

A brief calculation shows that resonance is indeed the phenomenon concerned with self-excitation. The 33 kV line shown

in Fig. 3 has a capacitance of $0 \cdot 225\,\mu$F, giving a reactance of 14 kilohms at 50 c/s. Fig. 4 shows the generator at 50 c/s and 420 volts to have a magnetizing current of 140 amp, and thus an inductive reactance of 3 ohms or (converted to 33 kV base) 18 kilohms. With the transformer reactance in parallel, the net inductive and capacitive reactances become closely comparable. It is worth noting that magnetic saturation is necessary for stable operation, since an unsaturated machine could, in theory, increase its voltage without limit.

It is implied at the end of Section 2.2 that a high capacitance leads to over-voltage and over-frequency. If a capacitance is high it will resonate with a given inductance at a low—possibly sub-normal—frequency. In Section 3.1.1 the same error appears to be made. A small machine has a large inductance, and will resonate with a lower line capacitance than a big machine.

The discussion of transient phenomena at the end of Section 3.2.2 deserves elucidation. It is not easy to see why power-interchange 'sets up another transient'. Why is it not part of the same one? This would make an interesting analytical study.

Mr. J. C. Beverley: Two of the reasons for the economic advantages of induction generators appear during the course of the paper. The first is that the exciter, voltage regulator and field switching equipment can be deleted. This amounts to about 5% of the cost of a machine.

The next advantage is that a turbine driving an induction generator does not require a governor, which means that with the Francis type of turbine the pressure pumping equipment and servo motors can be left out. These are replaced by an electric motor which can close the turbine gates and open them to adjust the load. This leads to a substantial economic advantage of up to 10% of the cost of a turbine.

A third advantage arises from the second and it is not so immediately apparent. Alternators driven by governed turbines often have high flywheel effects to meet hydraulic conditions, and this requirement is removed when the governor is left out, which, in the case of the Sron Mor station produced a saving of about 20%. It is clear from these three features that induction generators often offer real economic advantages, and yet the Sron Mor generator is, I believe, the highest-output machine of this type in the world. Since 100 MW machines have been talked about it will be interesting to know why induction generators larger than that at Sron Mor have not been adopted.

When switching in induction generators, a wide discrepancy from synchronous speed is acceptable. Can the author state, therefore, why a stroboscope is used for speed matching! Would not a frequency indicator be sufficient? Tests have also been carried out switching in 22 MW synchronous alternators on to the system when stationary and with the field switch open, and it has been proved from these tests that salient-pole alternators can be started up in this way provided that there is an adequate damper winding. This will prove useful in considering machines for pumped-storage installations.

Mr. D. D. Stephen: The method of starting induction generators by accelerating them to near synchronous speed using the hydraulic turbine is presumably only used to reduce the duration of the starting current. The transient peak current and its decrement time are essentially constant irrespective of the actual rotor speed. The steady-state current, which is a function of the rotor speed, is fairly high for slips much above 10% and approaches the no-load magnetizing current at almost synchronous speed. Those superimposed phenomena can be detected in the current characteristics shown in Fig. 8.

The different peak values indicated in Table 1 are probably associated with the actual phase angles at switch on, and the values of peak-to-peak current when extrapolated back to zero time would doubtless be much more constant.

Since the voltage dips produced would be of the same order if normal direct-on-line starting were used, would that method not greatly simplify the problem associated with hydraulic starting, particularly if this is automatic, as indicated in Section 7.2? This would also avoid the need for synchronizing equipment.

The need for low-voltage protection is not clear, since reclosure of the supply could not be more severe than a direct-on-line start which would not be expected to damage a machine. Similarly restoration of the supply with the machine at over-speed would not appear to be dangerous. Presumably no attempt is made to protect against this condition. The only condition likely to prove dangerous is when the machine is self-excited by the system and the machine voltage remains high. Restoration of the supply can then subject the machine to a 'double' short-circuit with a good possibility of its being damaged. This condition, of course, is not prevented by the provision of a low-voltage relay.

Table 2 shows many machines of low speeds, of 200 or 300 r.p.m. and as low as $113 \cdot 5$ r.p.m. Recent investigations have shown that at such speeds induction machines are not always the most economical devices. Synchronous machines, which are smaller and lighter owing to their smaller apparent power for the same active power rating, are more efficient, have lower apparent power and hence starting current and smaller associated voltage dips, and they can also maintain rated output with much lower voltage dips in the supply than induction machines.

If the maintenance associated with brushgear is of great significance, would the availability of a brushless synchronous machine provide all these desirable characteristics without the disadvantage of brushgear, particularly for unattended operation and fixed loads?

No reference is made to change-pole machines being used for motor/generator units. Is this feature likely to be of any significance in the future in view of recent developments in single-winding change-pole machines with virtually unrestricted choice of speed?

Mr. H. E. Clapham: The initial switch-in current is dependent on the standstill impedance of the generator and is therefore independent of speed. When switching-in is carried out within $\pm 5\%$ of synchronous speed the duration of the switch-in current is at its shortest. The best practice seems to be to switch in when running slightly above synchronous speed. This applies particularly where gear-driven generators are concerned, so as to avoid backlash in the gearing.

I have never found it necessary to use devices to reduce the switch-in current. Where doubt exists, provision can usually be made for installing reactors at a later date if necessary. This policy was followed in the case of a 310 kW generator connected to a 415-volt factory system. It was calculated that without reactors the peak value of the first half cycle could approach 5 680 amp. This value somewhat alarmed the purchaser and the supply undertaking. However, site tests showed that the effect on the works 415-volt system was so extremely transitory that the extra cost and complication of providing reactors would not be worth while.

In speed matching prior to switching-in one is concerned with the difference between the generator speed and the synchronous speed as determined by the system frequency at that time. When the induction generator is fitted with an auxiliary permanent-magnet generator it is possible to arrange for the generator and system frequency to be displayed on a double-element frequency meter for switch-in purposes. Usually it is cheaper to use a simple stroboscope, which gives a direct indi-

cation of speed difference from synchronism. Such stroboscopes have the merit of always giving accurate indications, whereas frequency meters may become inaccurate as time goes on.

Mr. E. B. Cocks: Among other worthwhile possibilities, induction generators might find considerable use in a system where a large hydro-electric station which, set low in order to contain sufficient catchment area for storage purposes, left a great proportion of the catchment at higher altitudes where considerable potential power might be developed at a number of sites.

The civil-engineering costs of the development of such sites would, in general, be high compared with those of the generating plant. In such cases, therefore, the robustness of the induction generators would appeal rather than the proportionate saving over the cost of synchronous plant, as stated by Mr. Beverley.

Other speakers have touched on the economics of the Scottish induction generators. I do not think that the author intended, in Section 11, to indicate that the low cost of generation was primarily due to the use of the induction generators. The main civil-engineering work had already been done on the main scheme, which would account for a part of it. Can the author give the economics of the induction generating sets in the Scottish schemes compared with the more normal synchronous machines?

Mr. P. Richardson: The adoption of higher-voltage networks and possibly underground cables would aggravate the situation with regard to line charging in this country. Does the author visualize a further extension of induction generation in his own area, and can he see an application for steam-driven plant in this country? Has he encountered any obvious limitations?

A number of coarse synchronizing tests were carried out on the north-east coast using a 60 MW generator. The initial current was about three times the rated load current, and in certain of the tests the current/time relationship was similar to that in the lower record of Fig. 8.

Why were right-angle-gear-driven alternators adopted in certain instances, since I would regard this as both noisy and inefficient. While gearing efficiency is approximately 99%, the figure does not include the bearings or thrusts. Whereabouts is the thrust provided to take the weight of the turbine runner, as it must be carefully positioned to avoid the effects of thermal forces on the gearbox.

The over-voltages which can be imposed on an induction generator can be severe, and although over-voltage protection was provided, protective gear can fail and the generator should be designed to withstand the worst condition. Were any special pressure test levels adopted for the induction generators?

The induction alternator with a laminated rotor construction would appear to have two disadvantages; first, the possibility of travelling-wave harmonics, and secondly, the overheating of the end-rings under negative-sequence fault conditions. Were any harmonics encountered with this form of generation? What is the negative-phase-sequence capability of the alternator and was negative-phase-sequence protection provided?

Mr. W. L. Kidd: I should like the author to give some actual costs for induction-generator installations. In these days of capital restrictions and joint planning the installation of a hydro-electric set means that the equivalent capacity of steam plant will not be installed. The economic study should take account on the one hand of the lower interest and depreciation charges associated with hydro-electric plant and on the other of the higher load factor of steam plant. If the load factor of the hydro-electric plant is less than 70–80%, the effect of its installation instead of high-efficiency steam plant is to require all the existing steam plant to operate for longer hours to produce energy corresponding to the difference in load factor of the hydro-electric and steam plant with which it is being compared. Furthermore, while the induction generator can be credited with the ability to absorb line-charging current at light load periods, it must be debited with the cost of supplying reactive power for excitation at peak load times when reactive load is an embarrassment to the system.

In south Scotland we have been considering the relative economics of steam and hydro-electric generation, and find that at present a hydro-electric scheme must cost less than £120 per kW for 30% load factor or £60 per kW for 10% load factor, otherwise it is more economic to install coal-fired steam sets at £50 per kW. Some induction-generator schemes may fall within these limits, particularly if it is a case of installing a small set in place of a compensating water outlet valve. Such an installation is being considered in south Scotland.

With regard to line-charging requirements, the problem in Scotland will be considerably aggravated in the next three or four years because the requirements of the Southern Board's lines are expected to increase to 200–300 MVAr of line-charging power at off-peak times after full use has been made of the reactive output from any synchronous plant running at night. These requirements are so large that the use of induction generators is unlikely to have any significant effect.

Mr. N. C. Adcock: It is interesting to note the increasing use of induction generators by the North of Scotland Hydro-Electric Board. In addition to those given in Table 2, other induction generators are in use in Scotland. My company supplied a water-turbine-driven induction generator in the late 1920's, which is rated at 80 kW, 515 r.p.m., 3-phase, 50 c/s, 380 volts, and is still in operation. Doubtless many other privately owned units are in existence.

The statements in Sections 2.2 and 3 on the dangers of over-voltage and self-excitation and the need to study these problems before installing an induction generator are real, but they are not restricted to water-turbine-driven machines. Over the past five years a batch of 35 induction generators with ratings of up to 100 kW, 394 r.p.m., 3-phase, 50 c/s, 400/440 volts have been or are being supplied. They are belt-driven from oxygen-plant expansion engines, which, in common with the water turbine, require a suitability for over-speeds of about 200%.

In 1957 site switching tests were made on the Sron Mor set mentioned in Section 10.3 of the paper. Some of the results are given in Fig. A.

The voltage traces commence at switch-in only, and the initial system voltage is not recorded.

Reference to Fig. 18 indicates that the Mullardoch generator torque/speed characteristic is practically linear up to about 200% full load, and this tends to give a similar recovery time irrespective of switch-in position up to $\pm 10\%$ slip. At this slip the turbine net torque is zero provided that the speed of the set is maintained steady and is very small at near synchronous speed and also provided that the turbine gate setting remains constant. Following the transient conditions which have been previously discussed, the current will fall to a steady-state value corresponding to a point near switch-in slip, and in this instance resulting in a generator torque somewhere near its maximum value. The total generator torque developed is therefore used to reduce the speed by approximately 70 r.p.m. When switch-in takes place at $\pm 5\%$ slip, both the torque developed and deviation from synchronous speed are halved, which results in a similar recovery time for the two conditions.

When a generator installation is being conceived it should be made clear whether or not the induction machine is to be used as a motor to bring the set up to sub-synchronous speed and also whether it will be required to reduce the speed to sub-synchronous following an over-speed. If these duties are not required the rotor cage winding can have a very low resistance,

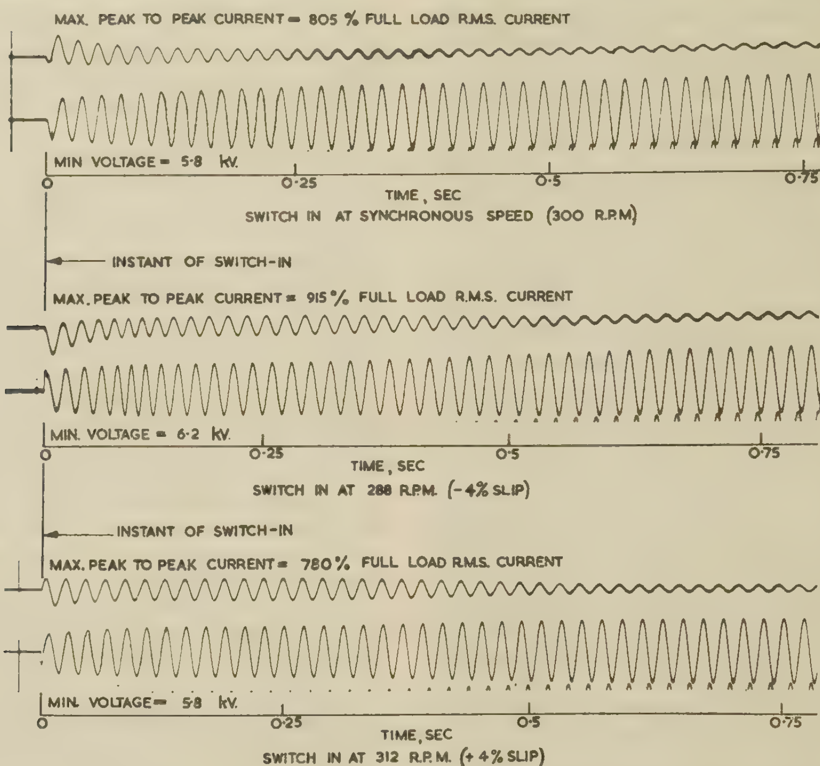

Fig. A.—Some of the results of switching tests made on the Sron Mor 5 MW 11 kV induction generator.

giving a higher efficiency than that of an equivalent rated induction motor.

Mr. C. E. Stille: In Section 6.2 it is stated that the over-speed device is set at some figure reasonably above the normal running speed. Some years ago I wondered whether values of about 30% were justified. In view of the difficulties with over-speed conditions, does the author's experience with the machines confirm the need for values of this order? A low setting of 5–10% for circuit-breaker trip operation with this circuit open until after the start would appear to be both desirable and feasible.

In Section 7.2 there is a comment on the automatic control during starting of Kaplan turbine machines. Some amplification of this Section is desirable in respect of the influence of blade angle and guide-vane opening on starting conditions. A given standstill torque, sufficient for starting, can be achieved only by a guide-vane opening in excess of that required for no load at normal speed with the usual automatically maintained guide-vane-opening/blade-angle relation. With the runner blades temporarily retained in the fully open position, a greatly increased standstill torque is achieved with this no-load normal-speed guide-vane opening, and, depending on the state of the thrust-bearing oil film, sufficient torque would be produced for a start with this or only a slightly increased guide-vane opening. The added complication of achieving a temporarily blocked return to normal blade-angle control during a start could lead to an overall simplification of the automatic control scheme.

Mr. K. J. Eales: I feel that the remarks in Section 6.2 on over-speed could be misleading to anyone unfamiliar with the behaviour of induction generators driven by water turbines. They give the impression that excessive over-speeding is prevented by the installation of an over-speed device. This was certainly not true of an induction generator with which I was recently associated, since, on full-load rejection, the runaway speed was attained in 10 sec and continued for a further 45 sec before it showed any signs of decreasing speed. This result is, of course, not surprising when it is realized that the guide-vane closing time is of the order of 3 min.

With regard to Section 9.3 I should like to have more information on the test for phase rotation using the 2-phase connection, since I do not understand the term 'twice the normal voltage'. I have been led to understand that twice the *phase* voltage will appear if the phase rotation is incorrect, but in a recent test using this method I witnessed a reading of 2·5 and 0·4 times the phase voltage for incorrect and correct phase rotation, respectively. After a study of this problem I believe that, owing to the neutral displacement between the two voltage systems, the theoretical value for incorrect rotation is three times the phase voltage. As this latter value is to some extent supported by the test mentioned above, I should be interested to know whether the author's figure is derived from actual tests carried out on site.

Mr. R. G. Smith: In Section 11, which deals with economics, there is a reference to inter-reservoir installations. In the Snowy Mountains Hydro-Electric Scheme there are two main inter-reservoir tunnels, and the difference in head between the reservoirs, and the flow is such that about 20 MW could be generated. The Authority looked into developing this power, and induction generators were considered because of simplicity, ease of control and cheaper installation, particularly as there would be no voltage regulators or governors. In both cases the outfall works were such that it was necessary to consider underground stations, with their higher cost in civil engineering works. In addition, water flow through the tunnel could not be interrupted when it is necessary to shut down the generator, so that a full-flow by-pass valve is necessary. This would also be used as a relief valve in the event of load rejection. Further investigation showed that low-speed water turbines were desirable, and thus the induction generator became less attractive. The scheme was not adopted on economic grounds.

Mr. P. L. Olsen (*communicated*): The Introduction to the

paper implies that the main reason for employing induction generators is that they are less expensive than synchronous machines and have simpler control and auxiliary arrangements. Section 11 refers to savings in capital cost, but is the author able to give comparative costs for a typical installation or an approximate overall saving in cost for the 20 MW of plant installed up to the present? The simplicity of the induction-generator unit and its control gear is confirmed by the absence of a Section in the paper dealing with difficulties experienced. The only hazard in operation appears to be the possibility of dangerous over-voltages arising from self-excitation when load is thrown off at the far end of the connected transmission system. It will be interesting to know how many installations are subject to this hazard and if any breakdowns have occurred.

In Section 3.1.3, which gives an example of excitation from system capacitance, the tests on the 350 kW machine confirm that dangerous over-voltages can be reached, but it is not clear why a voltage of 150% of normal was attained when the over-voltage protection was set at 120%.

In the example selected, i.e. the compensation-water set at Quoich Dam, it would have been possible to avoid system excitation by increasing the air-gap of the generator. By doubling this air-gap the voltage build-up indicated in Fig. 6(*a*) would have been prevented, and for the conditions illustrated in Fig. 6(*b*) the voltage would not have reached 625 volts until 47% over-speed had been reached instead of 27% as indicated. On the other hand, the increase in the air-gap in this particular case would have increased the full-load power factor from 0·84 to 0·77 approximately, giving an increase of 9% in the apparent-power rating of the generator with some increase in cost. However, it is noted in Section 4 that an increase in magnetizing apparent power is relished during night-time conditions, and it would appear that there is some merit in providing large air-gaps in induction generators for such systems. It is also possible that the over-voltages could be further restricted by arranging to saturate the magnetic circuit to a greater extent.

In any case the generator designer should be made aware of the system conditions.

Mr. D. Rudd (*communicated*): The primary purpose of a governor on any prime mover is to regulate the flow of fluid (whether steam, water or fuel) through the prime mover in accordance with the variations in the load on the machine. An isolated induction generator, however driven, supplying a variable electrical load, would require a governor, but it would have to be sensitive to frequency instead of speed.

If, in accordance with normal modern practice, a large number of generators (whether induction or synchronous) are operated in parallel at constant frequency, the frequency being accurately controlled at a selected control station, the governors no longer fulfil their primary function and are reduced to the following secondary roles:

(*a*) If the control station is unable to cope with a load variation, e.g. if the variation is too large or too sudden, the frequency will vary slightly and the governors will share out the variation among all the machines until control can be regained.

(*b*) If a generator or group of generators is temporarily isolated from the main system, their governors will operate to regulate the frequency until normal conditions are restored, and will prevent over-speeding of the machines.

Having provided governors, they are then used for speed control during synchronizing of synchronous generators, but direct fine control of the valve or vanes which regulate the flow of fluid would serve the same purpose, except possibly on very-large-capacity machines.

There is thus no argument for omitting the governor on a small induction generator which does not apply equally to a synchronous generator. But the author gives this omission as the first (and therefore presumably the most important) saving in capital cost, arising from the use of induction generators. Would it not be fairer to remove this item from the list and rest the case for induction generators on the other aspects discussed in the paper?

[The author's reply to the above discussion will be found on page 549.]

NORTH-WESTERN CENTRE, AT MANCHESTER, 1ST MARCH, 1960

Mr. H. Headland: While mechanical and electrical engineers on hydro-electric work are always conscious of turbine and alternator efficiency, the paper demonstrates that overall water economy is important where statutory or catchment operations impose conditions which could be wasteful. The hydro-electric energy generated by the North of Scotland Hydro-Electric Board in 1958 was 1 683 million kWh with an installed capacity of 813 MW, giving an annual plant load factor of 23·6%. The output from these relatively small induction generators was 64 million kWh and 20 MW or 3·8% and 2·5%, respectively, which is a significant contribution to overall hydraulic efficiency.

These small stations are justified by the high load factors associated with compensation water and reservoir regulation. An analysis of Table 2 shows these to be about 60% and 40% respectively, corresponding to about 7 500 and 5 000 operating hours per annum. Confirmation of these estimated running times, preferably for individual stations, would be helpful.

The output in terms of fuel economy might also be mentioned. If 64 million kWh/annum corresponds to 1·23 lb/kWh in thermal stations, the coal conserved amounts to 35 000 tons/annum, and at £3·6 per ton, the saving is £125 000 per annum. This corresponds to a fuel cost of 0·475d./kWh. The hydro-electric energy generated by the North of Scotland Hydro-Electric Board in 1958 cost 0·727d./kWh, and in Section 11 the author reports that these small stations produce energy at 30–50% of that at the main stations or, say, 0·325d./kWh. It is, perhaps, important to realize that the associated civil-engineering works might, in most cases, be necessary in some other non-productive form, so that real overall costs are relatively low. A selection of small machines totalling 1 125 kW in operation averaged £27·5 per kW and produced 5·6 million kWh/annum; taking the annual charges at 7·5% the machine energy cost component is 0·10d./kWh, and so that for the civil-engineering work seems relatively high. These installations are justified, but could the author say whether any cost-allocation system has been devised for civil-engineering and other works which may be necessary with and without generating plant?

In the early days there was some concern about the effect of induction generators on system operation, but the paper demonstrates that these fears were unfounded. Where conditions are suitable larger machines are now envisaged if they can compete with synchronous plant. The 4 MW unit at Dalchonzie, for example, has an overall efficiency of 86·6% as against about 88% for conventional plant. The author's views on whether there might be an economic or technical limit on induction-generator rating, fixed perhaps by hydraulic machine or other considerations, would be interesting.

The author's emphasis on the rugged simplicity of these machines might be more convincing if associated with availability figures and the causes of outage, since these should indicate

where design improvements are desirable, and some of the comparatively expensive automatic by-pass arrangements might be simplified if the risk of failure to meet statutory or operating requirements could be assessed. Operating experience with the three geared units and their effect on efficiency might be mentioned, since this could be relevant to larger turbines and tubular units.

Table 2 includes water turbines of all types for varying operating conditions and speeds. Speed is related to turbine head and output, and while most units are consistent for the type adopted, an exception is that for the Vaich tunnel. Were there special reasons for the low speed of 113·5 r.p.m.? Is the 70 ft head correct, or did difficult suction conditions or a wide discharge or head range apply where a Kaplan turbine without gears might have been appropriate?

Incidentally, the 125 kW Duchally turbine is not a conventional Francis but a special impulse wheel designed to bridge the specific speed gap between Pelton and reaction turbines.

An important omission is any reference to alternator performance. For small machines we are obtaining test efficiencies between 94 and 95%. Turbine efficiencies ranging from 80 to 86% are less well established because verification by normal flow-measuring procedures is expensive, and some thought to simpler methods seems appropriate. I suspect that these figures may be low, and, in this connection, would the author indicate whether the average energy outputs in Table 2 are based on meter readings, and if so, how do they compare with the project estimates?

Mr. A. Holgate: The tests at Quoich and Mullardoch have provided results of considerable interest. In particular, the switch-on tests at speeds away from the synchronous speed of the induction generator have demonstrated a phenomena which is rarely observed. It would be useful to have the generator equivalent-circuit parameters.

The steady-state tests in Fig. 5 show very close agreement with the calculated values. If, in addition to the equivalent circuit, the inertia constant and the speed/torque curve of the turbine at the relevant guide-vane opening were known, the curves in Fig. 6 could also have been checked by calculation.

The switch-in tests (Fig. 8) can also be readily checked by calculation. The a.c. component of the switch-in current is dependent only on the line conditions and the sub-transient reactance, $X_{a''}$, of the generator. The d.c. component is determined by the instant of switching. From Fig. 17(*a*) the starting impedance can be obtained, and if there is no appreciable skin effect in the rotor bars this value will be slightly less than $X_{d''}$. The shape of the speed/torque curve (Fig. 18) suggests that, in fact, there is negligible skin effect, and $X_{d''}$ has been assumed to be 0·16 per unit. If the stator and rotor resistances are estimated to be 0·01 and 0·008 per unit, respectively, the following values may be calculated.

A.C. component of the switch-in current = 5·2 × full-load current (peak).

Voltage at the generator terminals = 1·82 kV (r.m.s.).

The time-constants $\tau_{ac} = 0\cdot109$ sec and $\tau_{dc} = 0\cdot027$ sec.

If switching occurs at the voltage peak, a d.c. component of 14% of the a.c. peak will occur. The measured currents on the oscillogram show a d.c. component of 73% of the a.c. peak. This gives a current peak of nine times the full-load current (a.c. + d.c.), which agrees well with the test figures. The calculated generator voltage also shows good agreement with the test voltage.

The variation in the switch-in current observed is entirely due to the point of switching, and the averaging of the peak currents in Table 1 is clearly not acceptable.

The high resistance/reactance ratio of the supply results in a much faster decay of the d.c. than the a.c. transient. If the rotor is not at synchronous speed this will cause a beat frequency of $f_r - f_s$ to appear in the a.c. transient. For example, at 827 r.p.m. $f_r - f_s = 77$ r.p.m. or 5·1 c/s, and at 710 r.p.m. $f_r - f_s = 40$ r.p.m. or 2·65 c/s. This effect causes an apparent beat frequency in the a.c. envelope of the current. Two beat cycles can be detected in the second and third oscillogram at about these frequencies. This is probably the correct explanation of the phenomena. If the explanation given in the paper is correct it would be expected that a squirrel-cage induction motor would exhibit this peculiarity when starting on any system, but this has not been my experience.

Mr. J. Hindmarsh: Does the author consider that induction generators should be used to a greater, or a lesser extent in hydro-electric installations? Would he summarize the main reasons for choosing them in the first place?

[The author's reply to the above discussion will be found on the next page.]

SOUTH-EAST SCOTLAND SUB-CENTRE, AT EDINBURGH, 8TH MARCH, 1960

Mr. W. G. Crawford: The equivalent-circuit theory of the induction machine, so neatly outlined by the author in the Appendix, would appear to suggest that, if an induction generator is running exactly at synchronous speed when switched in, the switching transient would depend only on the impedance of the stator winding in series with the magnetizing impedance, because at synchronous speed the slip is zero and the impedance of the rotor branch is then infinite. The current at switch-in would then be the magnetizing current I_m with a superimposed transient term. The oscillogram of current flow following switching-in at synchronous speed shows that this is not so, and it is necessary to look much more deeply into induction-machine theory to find an adequate explanation of the observed phenomena.

A complete explanation can only be given in mathematical terms, and a brief verbal explanation must, of necessity, be incomplete. At the instant of switching the generator on to the line there is no flux in the machine, but after half a cycle or so there must be a flux linked with the stator winding. If the resistance of the stator winding is ignored, the magnitude of this flux must be such that as it varies it produces a back e.m.f. equal to the system voltage. This flux cannot penetrate quickly into the rotor because, as soon as it starts to do so, e.m.f.'s and currents are induced in the rotor conductors and oppose the penetration of the flux into the rotor.

Consequently, the flux is initially confined mainly to the leakage-flux paths in the stator. As the permeance of the stator leakage-flux paths is much less than that of the main magnetic circuit, there is a very high current inrush immediately the machine is switched on to the line. This current inrush decays very rapidly because the time-constant for this current decay depends primarily on the ratio of the leakage inductance to the resistance rather than on the ratio of the total inductance to the resistance.

I have had oscillograms taken of the switch-in currents on a slip-ring induction machine when driven at synchronous speed with the rotor winding open-circuited and short-circuited. With the rotor winding open-circuited, the switch-in current is much less than with the rotor winding short-circuited, and a fairly high alternating voltage is induced in the rotor. The switch-in

current wave is the highly distorted waveform normally associated with the switch-in of saturable reactor or transformers. With the rotor short-circuited, the switch-in current is considerably higher and shows little signs of distortion due to magnetic saturation, as is to be expected if the flux is initially confined mainly to the leakage-flux paths of the stator winding.

It is thus interesting to compare the rapid decay of the switching-in transient of the 2·4 MW generator at Mullardoch with the very slow decay of the generated voltage of the 350 kW generator at Quoich Dam after disconnection from the supply. In this case, the time-constant of the decay of voltage depends on the ratio of total inductance to resistance and is therefore fairly long.

Mr. H. E. Clapham: The Quoich tests clearly demonstrate that the policy of fitting over-voltage relays as standard is a sound one, and much superior to the practice of trying to design the system in such a way as to limit the capacitance which can be left connected to an isolated generator, for example, by the use of interconnected circuit-breaker trips. There is always the possibility that future alterations to the system may spoil the efficacy of precautions of this type, whereas an over-voltage relay would remain fully effective.

In Section 9.3 the author claims that the 2-phase connection is unsuitable for use where high-speed machines are concerned. I am unable to support this view, and, indeed, I have used this method recently on a 1 550 kW 1 515 r.p.m. 3 300-volt induction generator which exhibited no signs of distress whatsoever. In my opinion no machine 'worth its salt' should find this practice in any way distressing. This method has the merit of being so simple that it is almost impossible to make a mistake when using it.

Most large users of squirrel-cage induction machines have in the past been put to considerable trouble and expense by rotor-winding failures due to rotor bars breaking, often necessitating the fitting of completely new rotors, and sometimes involving also the complete rewind of the stator. In some cases the repaired machine would fail repeatedly. Such happenings on the many relatively large and remotely situated machines described in the paper would have been most serious and could have had much influence on future policy regarding the use of induction generators. Fortunately, the generators described have been built within recent years, making it possible in most cases to incorporate proved methods of rotor-winding construction developed in 1946, which make the chance of rotor-bar fracture so exceedingly remote as to be not worth considering.

THE AUTHOR'S REPLY TO THE ABOVE DISCUSSIONS

Mr. C. L. C. Allan (*in reply*): I will reply to the speakers individually.

To Prof. Say.—Presenting vector diagrams is a matter of choice, and external and internal viewpoints can be used for both motoring and generating conditions. Presentation was confined to the two arrangements thought to be most familiar to power engineers.

It is very interesting to see the treatment of the rotor resistance by considering it in two parts.

Capacitance does not control frequency, but if there is resonance, voltage will increase with frequency. A small machine (high inductance) requires less line length for resonance and so is more exposed to the risk.

Section 3.2.2. referred to magnetizing and power current transients separately because the current vectors are at right angles and are changing at different rates.

To Mr. Beverley.—I am grateful to Mr. Beverley for the figures he has given showing the worthwhile savings which can be made.

Larger sizes of water-turbine-driven generators often require to operate at low leading power factors or even as synchronous inductors in order to absorb more reactive power than an induction machine.

A stroboscope is a very simple arrangement. Indication of the system frequency is required as well as machine frequency.

To Mr. Stephen.—The easiest and the usual way to start a machine is by the turbine. To switch in the electrical end direct would cause a long and heavy starting current. In some cases compressed air would be needed to keep the runner clear of water, and in others, water would have to be admitted to the runner at nearly full speed.

It is correct to say that the different peak currents in Table 1 are associated with the instants of switch-in. The experiments were mainly to find the maximum currents and the duration of the transient.

Low-voltage protection is very necessary. Direct-on connection is not the normal arrangement, and the condition of stable operation at reduced voltage and high speed should be guarded against.

If an alternative supply is required for a network, a synchronous machine would be needed. In other cases an induction generator will do and would be installed so long as it was the most economic proposition.

To Mr. Clapham.—Switch-in at just above synchronous speed seems sensible and practical for the reasons Mr. Clapham gives.

He confirms that current-limiting devices are rarely if ever needed. I agree that it is the short duration of the transient which is most significant.

One needs to have both generator and system frequency displayed, and stroboscopes work well in practice.

To Mr. Cocks.—The type of generation described by Mr. Cocks would be suitable for induction generators. Robustness, simplicity and reduced cost would all be attractions.

Induction generators have been used both for ordinary hydro-electric stations and for the type where a relatively small addition can be made to a main construction work such as a dam. In all cases the lower cost of an induction generator is an attraction, but it is only where the scheme is a relatively small addition to a main constructional work that the costs of electricity production are very significantly reduced.

To Mr. Richardson.—A synchronous water-turbine-driven alternator has the advantage over an induction machine of being able to vary its reactive power. We shall certainly continue to use induction generators for reasons of simplicity and economy, but larger machines in some stations will need to be synchronous in order to control the effects of h.v. system capacitance currents.

No special voltage test levels were specified for induction generators. Over-voltage protection was considered an adequate safeguard.

No difficulties due to travelling-wave harmonics have occurred. Phase-unbalance protection gives a measure of safeguard against the effects of negative-phase-sequence current. Probably up to half the full-load current with one phase disconnected could be safely carried.

To Mr. Kidd.—The load factor of a coal-burning station starts off by being high but declines during its lifetime. To calculate the equivalent initial capital cost of the hydro-electric stations to be of the same merit as a steam station is even more difficult. Apart from load-factor problems, there are differences in 'other works' costs and, more significantly, there is the cost of fuel (likely to increase) for the thermal station, which the hydro-

electric station does not require. The life of a hydro-electric station is several times that of a thermal station. To allow properly for all these factors and to bring them back correctly to the present-time capital-cost equivalent is not easy. The figures I have taken out for the justifiable cost per kilowatt for hydro-electric schemes on this bases come out considerably higher than those given by Mr. Kidd.

Owing to the long length of overhead lines, synchronous generators in the north of Scotland operate fairly near unity power factor, even at peak load times, and no debit for reactive current arises. What the paper tries to show is that on the north of Scotland system the lagging excitation current required by induction generators causes no difficulty.

To Mr. Adcock.—Certainly the use of induction generators need not be confined to those driven by water turbines, and it is interesting to have details of the machines described by Mr. Adcock.

I am grateful to him for providing the switch-in results for the Sron Mor machine. These show similar features to the Mullardoch tests.

So far all machines are arranged for starting from the turbine, and I would certainly favour the high-efficiency design.

To Mr. Stille.—Over-voltage protection settings of about 120% have been satisfactory. Circuit-breaker over-current and thermal protection is time-lagged and does not operate on the short-switch-in transient.

To Mr. Eales.—It is true, as Mr. Eales says, that slow guide-vane closure times cause over-speed and sometimes runaway speed. Over-speed protection operates at an early stage, to initiate shut-down.

The paper should have stated that a small voltage will appear with correct phase rotation in the test described in Section 9.3. The difference between the voltages measured with correct and incorrect rotation is clear. On the Pitlochry 50 kW machine measurements gave 0·42 and 2·7 times phase-to-neutral voltage for correct and incorrect rotation, respectively.

To Mr. Smith.—It is very interesting to have Mr. Smith's description of the investigation carried out on the Snowy Mountains scheme. The project did not go ahead on economic grounds, which suggests that it was too costly either with an induction or a synchronous machine.

To Mr. Olsen.—The figures given by Mr. Beverley are a good guide to the savings possible by using induction machines. Total installation costs vary with the amount of civil engineering associated with them.

There is often more than one switching point where disconnection could occur, and although the events required to produce over-excitation are in some cases unlikely, probably all machines are at risk. As no breakdowns have occurred, we are well satisfied to use over-voltage protection.

Regarding the tests described in Section 3.1.3 the over-voltage protection operated at 120% while the speed was rising. By the time the circuit-breaker had completely cleared the machine from the system, the voltage had risen to 150%.

Mr. Olsen states that, while a machine with a large air-gap would have a smaller voltage rise, its operating power factor would be worsened. Synchronous water-turbine-driven alternators are fitted with over-voltage protection in the case of automatic-voltage-regulator or excitation-control trouble, and similar protection on induction generators seems to be all that is necessary.

To Mr. Rudd.—I am interested in the points Mr. Rudd makes about governing. An induction generator is unlikely to be used to supply an isolated electrical network because of excitation difficulties. A synchronous machine used for this purpose would need a governor, and the system frequency and machine speed would be the same.

It is an oversimplification to suggest that a large interconnected system can be completely regulated by one station.

It is true that one sometimes finds machines set to operate at a fixed output by using the load limiter. In this condition the governor is not speed-sensitive, but the same machine may, at another time, have to operate with speed sensitivity. Nevertheless, the omission of speed-sensitive governing seems possible on some synchronous machines,

To Mr. Headland.—Mr. Headland emphasizes the relatively high load factor of compensation water and inter-reservoir schemes, compensation water stations being higher. Because of the significance of civil-engineering costs, every case really has to be examined individually to test its economics.

Efficiencies for induction and synchronous machines are usually about the same. Excitation circuit losses should be allowed for, and in the case of Dalchonzie these losses and the effect of a relatively high thrust-bearing loss (not really associated with the type of generator) suggest that the difference in efficiency quoted is too large.

Synchronous machines were preferred for recent 10 MW machines to obtain the maximum absorption of reactive power at leading power factor. There might be waveform difficulties with large machines, but I am inclined to think that the induction generator's lack of control on the reactive side will limit the size used.

Installation of by-pass arrangements for compensation water flows is required, because faults on overhead lines would disconnect the load from the machine and so reduce the flow too much. Gearbox efficiencies are about 96–97% and the arrangements work well. The Vaich-machine normal net head is about 23 ft. The maximum gross head will rarely be reached.

Output figures in Table 2 are estimations from water-flow availability. It is only on compensation water flows that meter readings in any one year would mean very much as a comparative check. On other schemes annual outputs will vary with rainfall.

To Mr. Holgate.—Mr. Holgate's analysis of the currents during the switch-in transient at Mullardoch seems correct. Beat frequencies were not considered at the time of the tests but their effect may be present. Acceleration or deceleration of the rotor must also take place. Perhaps both factors contribute to producing the current outlines.

To Mr. Hindmarsh.—There is not much which can be added to the reasons given for selecting induction generators. From experience gained so far I would say we are satisfied with the machines we have used in the places they have been used.

I think we would use induction machines for installations below about 5 MW unless they had to act as standby for supply to a part of the network. For the reasons given in the replies to Messrs. Beverley and Headland it is not so clear that larger machines would be induction.

To Mr. Crawford.—The equivalent circuit will apply to steady-state conditions but will not deal with transient ones. I am grateful for the explanation of the differences in times of current decay and in waveform, depending on whether the rotor is open- or short-circuited. The explanation of the difference in time of current decay depending on whether leakage or total inductance is the main influence is helpful, and its application to the Mullardoch and Quoich tests is illuminating.

To Mr. Clapham.—I agree with what Mr. Clapham says about operation and protection. I am glad to have his statement that any extra stress on a high-speed machine produced by the test described in Section 9.3 will not be severe.

There have been no troubles from rotor bars breaking, and I am glad to learn we may expect none. Induction generators have been noticeably trouble-free.

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THE RESISTANCE OF SHEET INSULATION TO SURFACE DISCHARGES

By J. H. MASON, Ph.D., Associate Member.

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SUMMARY

Investigations show that a satisfactory classification of insulating materials with respect to their resistance to deterioration and breakdown by surface discharges in air is given by time-to-breakdown tests using simple rod and plate electrodes. It is necessary to circulate dry air over specimens to avoid the formation of semiconducting films, and comparison should be made only between specimens of similar thickness. The discharge resistance of most materials is reduced by increasing the ambient temperature or by applying mechanical strain. Results of tests on thin films and sheets of several polymers and on both glass and paper laminates of 1–3 mm thickness are given.

Recommendations for standard tests for comparing the discharge resistance of materials are given in the Appendices.

(1) INTRODUCTION

In recent years it has become widely recognized that many failures in electrical equipment are caused by discharges in cavities or over the surface of the insulation. Ideally no discharges should occur at the working stress, but at power frequency, deterioration is often so slow that some discharges may be permissible unless very long life is required or the cost of premature failure would be excessive. Many organizations are therefore investigating possible methods for predicting the life of insulation and assessing the relative resistance of materials to discharges under service conditions.

It is now known[1] that the type and rate of deterioration by discharges vary with the thickness of the material, the amplitude, waveform and frequency of the applied voltage, and with the temperature, humidity and nature of the ambient medium. Thus no single test can reproduce the behaviour of a material under all conditions of service. In these circumstances it is best to adopt one or two standard tests which simulate certain typical working conditions and give reproducible results which can be related to subsequent service experience.

It should be noted that short-duration electric-strength tests,[2] which are widely used to check the quality of insulation, give little indication of the relative behaviour of materials in service. This is because breakdown in these tests occurs at much higher stresses than those applied in service, and failure is caused either by thermal instability or, in better-quality materials, by discharges at the edges of the electrodes. Above a critical stress these discharges penetrate the surface and propagate rapidly through the material causing failure beyond the periphery of the high-voltage electrode, as shown in Fig. 1. Tests on sheet insulation are usually made under oil, to avoid flashover, and the breakdown voltage is often more dependent on the electrical quality of the oil than of the solid specimen, particularly in tests at 20° C when the breakdown voltage is raised by ionic contamination in the oil.[1] Short-time breakdown tests in this country are normally made at 90° C, when the effects of contamination are reduced and there is a greater probability of thermal failure due to ionic

Fig. 1.—Partial breakdown channels in 3 mm-thick phenolic-resin-bonded paper sheet after 1 min step-by-step electric-strength test in oil at 20° C using British Standard electrodes.

losses in the materials or to cumulative heating by discharges. The results of these tests are little dependent on erosion or chemical degradation by discharges, which are the usual cause of failure at the lower stresses used in service.

Thus indications of chemical damage, caused by discharges at working stresses or the action of ozone and nitric oxides formed by discharges in air, might appear to be the best basis for comparing the resistance of materials to discharges.

Thomas,[3] however, showed that evidence of overall deterioration, such as increased acidity or dielectric loss, or decreased mechanical strength, often gives no indication of imminent failure, which is usually caused by severe local erosion, cracking or the propagation of discharge channels through a structural defect in the material. Thus the relative resistance of materials to these types of breakdown can be assessed only by comparing the times for samples to fail when they are subjected to discharges under specified conditions.

Thomas[4] suggested that discharge resistance tests should be made using a high-voltage electrode in the form of a stainless-steel truncated cone, with the apex in contact with the specimen mounted on a plane electrode. Preliminary tests with these electrodes were made on thin films of several polymers for periods of up to two hours at 50 c/s a.c., but it was evident that much longer tests were necessary to compare the resistance of materials to discharges at the lower stresses used in service. However, the scatter of results greatly increased with the time of test, because films of water or nitric acid, generated by the discharges, accumulated on the test surface and temporarily short-circuited the discharges. This effect was greatly reduced when dry air was passed continuously over the test surface, but the shape of the truncated-cone electrode prevented effective ventilation, and subsequent tests were made therefore with $\frac{1}{4}$ in-diameter rod high-voltage electrodes. These electrodes facilitated ventilation, which minimized the scatter in results and also gave the shortest average life for any material at a given stress and temperature. Thus materials are classified with respect to discharge resistance under the most severe conditions.

The results of time-to-breakdown tests using four electrode systems are discussed in the paper, and their merits for testing sheet and film insulating materials are considered. The procedure is simple and the reproducibility of the results and the clear

The paper is based on E.R.A. Reports Ref. L/T379 and L/T394.
Dr. Mason is now at the A.E.I. Research Laboratories, Harlow.

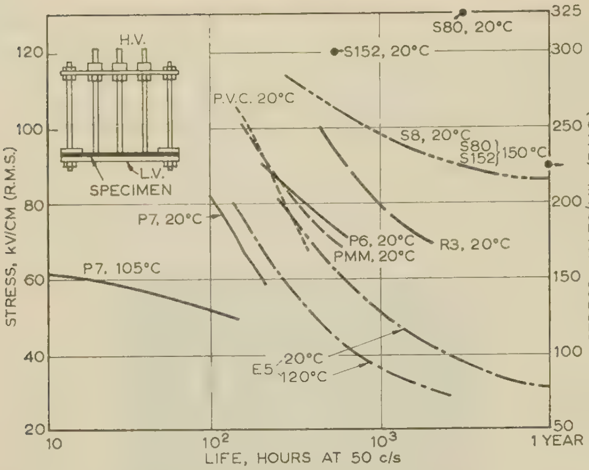

Fig. 2.—Discharge resistance of sheet polymers compared with that of representative glass and paper laminates.

Tests in dry air between 6·35 mm-diameter rods and a plane l.v. electrode.

Code	Material	Thick-ness	Number of tests	E_i at 20° C	Life at 50 c/s at 75 kV/cm 20° C	Life at 50 c/s at 75 kV/cm 120° C
		mm		kV/cm r.m.s.	hours	hours
S80 S152	Silicone rubbers	1·1	8	20 ± 1·5	$\geqslant 10^4$	$\geqslant 10^4$
S8	Silicone-glass	1·1	12	20 ± 2	$>10^4$	$>10^4$
R3	Linear polythene	1·6	15	18·5 ± 1	$1 \cdot 3 \times 10^3$	350 ± 25
P.V.C.	Polyvinyl chloride	1·5	15	14 ± 1	300 ± 150	—
P.M.M.	Polymethyl methacrylate	1·5	12	14·5 ± 1	400 ± 25	—
E5	Epoxy-glass	1·5	25	10·2 ± 1	320 ± 50	160 ± 25
P6	S.R.B.P.	1·6	6	9·7 ± 1·5	500 ± 50	—
P7	S.R.B.P.	1·6	15	10 ± 1	125 ± 25	$\ll 0 \cdot 1$

E_i = Discharge inception stress.

differentiation between materials, shown in Figs. 2, 4 and 7, indicate that they could be used as standard methods. The test procedure and the preferred electrode systems are therefore described in some detail in Section 8. It must be emphasized that correlation between the results of these tests and the behaviour of materials in service has still to be established, and the results should not be used for design purposes without consideration of differences between test and service conditions.

It is important to state the scatter in results in reports on the discharge resistance of materials. The scatter in results for sheet insulating materials is indicated by the standard deviation in life at a given applied stress. In the case of thin-film materials sufficient results were available to calculate the coefficient of variation ϕ, which is the ratio of the standard deviation to the average stress,

i.e. $$[\Sigma (E - \bar{E})^2/n - 1]^{1/2}/\bar{E}$$

where E is the stress for a given life in any one test, and $\bar{E}$ is the average value of E over n tests. In practice, several values of life at certain applied voltages were determined; the local thickness of the specimen was then measured and the stress at each point of breakdown calculated so that life/stress curves could be plotted. The values of E and $\bar{E}$ were determined from these curves.

If the coefficient of variation is less than 10% the material can be considered homogeneous, and the test conditions satisfactory. Higher values of scatter may indicate an inhomogeneous material, damage to specimens when setting up the tests, or poor control of the test conditions, e.g. insufficient dry air passing over the test surface or a non-uniform temperature distribution in the test chamber.

(2) ELECTRODES USED FOR TIME-TO-BREAKDOWN TESTS

Extensive tests were made to establish the optimum procedure for assessing the discharge resistance of laminates, sheet and film insulating materials. Four electrode systems were investigated, and are referred to in the text as systems (*a*), (*b*), (*c*) and (*d*) in accordance with the following description:

(*a*) Stainless-steel rod electrodes of ¼ in diameter with flat ends and square edges, mounted perpendicular to and in contact with the surface of the specimen, which rests on a brass plane electrode of much greater area, as shown in Fig. 3(*a*) and specified in Section 9.1.

(*b*) Rod electrodes as in system (*a*), mounted perpendicularly above a brass electrode of cylindrical section, as shown in Fig. 3(*b*) and specified in Section 9.2. These electrodes were used to assess the effect of mechanical strain on the discharge resistance of laminates, as shown in Sections 3.2 and 3.3.

(*c*) This was identical with system (*b*) except that an air-gap of about 75 microns was included between the base of each rod electrode and the surface of the specimen, as specified in Section 9.3. The system was used to avoid possible mechanical damage to thin film insulation as described in Section 4.1.

(*d*) Stainless-steel rod electrodes of ¼ in diameter mounted horizontally above and tangential to the surface of thin flexible materials which were mounted on a brass cylinder of 1½ in diameter as shown by the inset in Fig. 9. A 75-micron air-gap was included between each rod and the specimen to avoid mechanical damage to the latter.

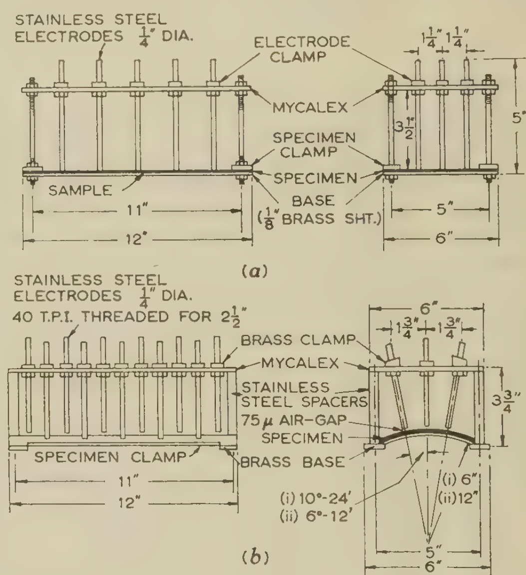

Fig. 3.—Rod and plate electrodes.

(*a*) Type A electrodes, for testing rigid sheet insulation.
(*b*) Type B and C electrodes; shown with an air-gap between each rod and specimen as used for testing thin-film insulation.

Method (*a*), which is the simplest, is shown in Section 3 to give reproducible results and satisfactory discrimination between different materials provided that the specimen thickness exceeds 0·5 mm. The method is not satisfactory for thin-film materials, in which the weight of the electrode causes mechanical strain, with consequent scatter in the results, and poor discrimination between materials.

Method (*d*) gives the same classification of thin materials as method (*c*) and a similar spread in results, but it is not described in detail since it is more complicated than method (*c*) and thus less suitable as a standard test.

(3) TIME-TO-BREAKDOWN TESTS ON SHEET INSULATION

The rod and plane electrodes of system (*a*) were used to compare the discharge resistance of both glass and paper laminates and sheets of various polymers. Results for materials of each group are considered separately.

(3.1) Discharge Resistance of Glass Laminates

The first object was to show that tests with these electrodes would give a broad classification between laminates impregnated with different resins. Various commercial samples were tested, although complete details of the impregnating techniques were not available. Since these techniques affect the mechanical properties of glass laminates[5] they are also likely to affect their resistance to discharges, and the results obtained should not be regarded as necessarily typical of the class of resin or as necessarily the best that will ultimately be achieved with that class.

It is desirable to accelerate tests as much as possible by raising the frequency or the stress above that used in service, provided that this does not affect the mechanism of failure. Preliminary tests showed that at 20° C there is no significant difference for most laminates between the life, in cycles, of samples tested at 50 or 500 c/s, provided that the tests are made in dry air. Samples of epoxy-glass laminates tested at 1 800 c/s, however, had a considerably shorter life (in cycles) than at 50 c/s, indicating failure by cumulative heating at the higher frequency. Results at 1 800 c/s would thus be misleading. Most tests were therefore made at 500 c/s, and the life was multiplied by 10 to give the equivalent value at 50 c/s. This procedure was also permissible at 120° C for epoxy- and silicone-bonded glass-fibre laminates, but with melamine-, phenolic- and polyester-bonded glass-fibre laminates, tests at 500 c/s and 120° C caused rapid breakdown by thermal instability, except at lower stresses; tests on these materials at 120° C were therefore made at 50 c/s. In all cases dry air was passed over the samples to remove nitrous oxides and water vapour.

Results of tests on five glass-fibre laminates are summarized in Fig. 4. Silicone laminate S8 has much greater resistance to discharges than the epoxy, melamine or polyester laminates. At 20° C and stresses below 50 kV/cm r.m.s. there is no significant difference between the two epoxy laminates, E5 and E3, and melamine M2, but the polyester, Y1, shows much less resistance to discharges. At higher stresses, E5 is clearly superior to E3 and E2. A temperature of 120° C had no appreciable effect on the silicone laminate, but the life of E5 was reduced to about a third, and that of M2 to about a tenth, of the life at 20° C.

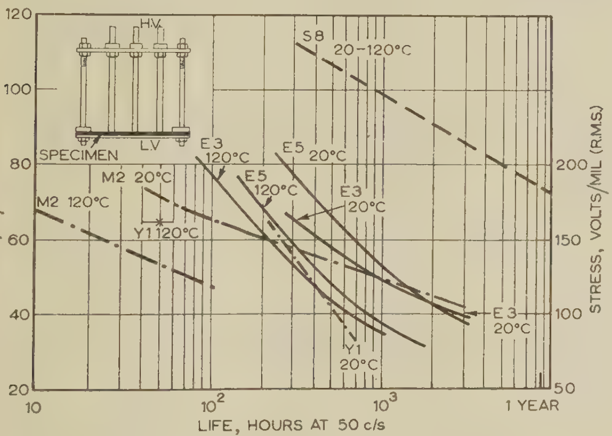

Fig. 4.—Effect of stress and temperature on the discharge resistance of glass-fibre laminates (1·6 mm thick).

The curves show the mean lives of specimens tested in dry air between 6·35 mm-diameter h.v. rod electrodes and a plane l.v. electrode.

Throughout the paper the 'average' stress applied to samples is quoted: this is calculated as the ratio of applied voltage to thickness of insulation at point of breakdown.

Code	Maker	Material	Temperature	E_i	Number of tests	Life at 50 c/s 45 kV/cm	Life at 50 c/s 65 kV/cm
			deg C	kV/cm r.m.s.		hours	hours
S8	*a*	Silicone	20–120	20 ± 2	5	~10^5	~2×10^4
E3	*b*	Epoxy	20	10 ± 1	5	1 800 ± 200	300 ± 30
			120	—	10	500 ± 35	180 ± 25
E5	*a*	Epoxy	20	10·2 ± 1	14	1 700 ± 250	540 ± 90
			120	6·3 ± 1	9	620 ± 110	220 ± 25
M2	*b*	Melamine	20	11 ± 2	6	2 000 ± 500	100 ± 10
			120	5·3 ± 1	8	130 ± 20	14 ± 2
Y1	*a*	Polyester	20	10 ± 1	4	460 ± 50	220 ± 20
			120	8 ± 1	3	—	50 ± 10

Exploratory tests have been made on some 15 other laminates, bonded with various resins and with different weaves of glass fabric. The resultant differences in discharge resistance are shown in Table 1. These results, later discussed separately for each resin, indicate a need to investigate the effect of different finishes for the glass fabric, and of different hardeners used with a given resin, to establish the conditions giving optimum bond between resin and glass and the greatest resistance to discharges.

It should be noted that, whereas the curves in Fig. 4 are based on at least two and often three or more samples tested at each of two or three different stresses (the number is indicated in the caption), some results in Table 1 are based on only one or two tests at a particular stress and temperature. Their significance is accordingly limited.

(3.1.1) Silicone-Glass Laminates.

Table 1 shows particularly striking differences between different silicone-glass laminates. Those with greatest discharge resistance are bonded with a resin having a high phenyl content, which adheres well to the glass fibre and is less liable to craze during cure than many other silicone resins.[6] If the resin-glass bond is poor or the resin crazes, discharges penetrate the fabric at quite low stress without preliminary erosion, so that the excellent discharge resistance of the silicone resin itself has no influence on the life of the laminate. Even laminates bonded with the same phenyl-containing resin show considerable difference between one manufacturer and another, indicating the need for improved impregnating techniques or possibly the use of a different finish for the glass fabric. It may be noted that the colour of the laminates gave no indication of electrical quality.

(3.1.2) Epoxy-Glass Laminates.

There is much less difference between the behaviour of different epoxy laminates than between the silicone laminates. Fig. 4 shows that the discharge resistance of E3 falls more rapidly with increase of stress at 20° C than that of E5, but at 120° C the difference between them is only just significant. It is noteworthy that E2, based on a coarse-weave fabric, has less resistance to discharges than the other epoxy laminates. Resin was initially eroded from the surface of all these laminates at about the same rate, but the open weave of E2 was then more readily penetrated than a closely-woven laminate.

Many epoxy laminates cured with an amine hardener become an olive green colour when heated in air. When tested at 120° C in the presence of ozone and nitric oxides generated by discharges, they become almost black after 100 hours. Samples previously aged for 100 hours at 120° C in air, or in the presence of ozone, showed no significant reduction in discharge resistance at 50 kV/cm and 120° C compared with unaged samples, so that the colour change appears to have little or no electrical significance.

Further results obtained with epoxy-glass laminates using electrode system (*a*) are shown in Fig. 5.

For laminate E5, variation of thickness from 0·8 to 3·2 mm has no marked effect on the life at a given temperature and stress.

Laminate E7 was an experimental sample using glass cloth with a Silane finish, impregnated with 100 parts of 828 resin plus 85 parts of MNA and 2 parts of BDMA hardeners. The cure was for one hour at 100° C followed by seven hours post cure at 100° C. The discharge resistance of this laminate was

Table 1

THE COMPARATIVE DISCHARGE RESISTANCE (C.D.R.) OF RESIN-BONDED GLASS LAMINATES BETWEEN 1 AND 1·6 MM THICK. THE TABLE SHOWS THE EFFECT ON LIFE OF VARYING THE IMPREGNATING RESIN AND THE WEAVE OF THE GLASS FABRIC

Impregnating resin and thermal classification‡	Code	Weave	Thickness	Electric stress	Life at 50 c/s*		Apparent c.d.r. classification
					20° C	120° C	
		Threads/cm	mm	kV/cm r.m.s.	hours	hours	
Silicones (H, 180° C) ..	S8a†	22 × 18	1·1	75	~10^4	~10^4	Excellent
	S3b	26 × 17	1·6	75	2, 160	—	Good
				45	>5 000	>5 000	
	S7a, S9d	22 × 18	1·1	45	<0·1	<0·1	Bad
Epoxy (B, 130° C) ..	E2b	14 × 11	1·6	75	—	90 ± 10	Fairly good
	E5a	22 × 18	1·5	75	350 ± 55	160 ± 25	Good
				55	900 ± 150	370 ± 50	
Melamine (B, 130° C)..	M2b	26 × 17	1·5	55	500 ± 100	45 ± 20	Good at 20° C, Poor at 120° C
	M3a	22 × 18	1·6	75	33 ± 7	—	
				55	—	0·01	
Polyester (B, 130° C) ..	Y1a	14 × 11	1·9	50	300 ± 50	> 50	Fairly good
Phenolic (B, 130° C) ..	P3a	14 × 11	1·8	65	5 ± 1·5	0·1	Poor

* The majority of tests were made at 500 c/s and the values of life given in cols. 6 and 7 were calculated as 10 times the life at 500 c/s.
† The second letter in the code signifies the manufacturer of the laminate.
‡ Thermal classification in accordance[18] with B.S. 2757: 1956.

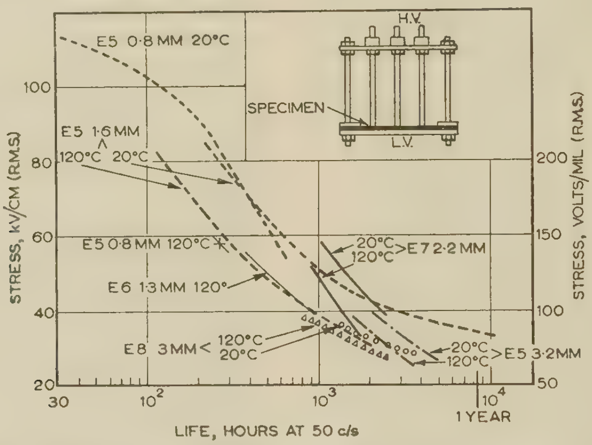

Fig. 5.—Effect of sample thickness on the discharge resistance of epoxy-glass laminates, tested in dry air between 6·35 mm-diameter rods and a plane l.v. electrode.

Code	Maker	Thick-ness	E_i		No. of tests	Life at 50 c/s			
						37·5 kV/cm		57·5 kV/cm	
			20° C	120° C		20° C	120° C	20° C	120° C
		mm	kV/cm r.m.s.	kV/cm r.m.s.		hours	hours	hours	hours
E5	*a*	0·8	17·7	13·3	10	—	—	600 ± 200	275 ± 25
		1·6	10·2	6·3	25	3 750 ± 400	1 100 ± 200	750 ± 150	325 ± 75
		3·2	5·3	3·2	22	2 600 ± 200	1 800 ± 150	—	—
E6	*e*	1·3	—	—	9	—	1 075 ± 75	—	400 ± 25
E7	*f*	2·2	9·5	—	18	2 400 ± 350	1 450 ± 50	1 150 ± 200	—
E8	*g*	3·0	7·5	—	16	950 ± 100	900 ± 650	—	—

comparable with that of E5 and E8, based on Volan-finish glass fabric impregnated with other resins. As E7 was transparent, and remained so when heated in the presence of discharges, it was possible to see that breakdown was preceded by the propagation of fine channels between, and in the plane of, the glass laminae. These channels were mostly in the weft direction of the cloth, and were apparently uninfluenced by numerous small bubbles trapped in the resin.

Laminate E8 was also an experimental grade, impregnated with a resin which was expected to give good properties at high temperature. However, Fig. 5 shows that its life at 120° C is somewhat shorter than that of E5.

(3.1.3) Melamine, Polyester and Phenolic Laminates.

Too few samples have been tested for adequate comparison, but the results in Fig. 4 and Table 1 show that the discharge resistance of both melamine and phenolic laminates falls very rapidly with increase of stress or temperature.

(3.2) Effect of Mechanical Strain on the Discharge Resistance of Glass Laminates

Previous investigations have shown that rubber,[7] polystyrene[8] and polythene[9, 10] in a state of mechanical strain have less resistance to discharges than unstrained material. A similar effect may be expected in other materials, and tests were made with glass-fibre laminates bent over the cylindrical electrode of system (*b*) shown in Fig. 3(*b*). If there is no plastic yield, the upper surface of a specimen bent in this way is subjected to a tensional strain of $t/2R$, where t is the specimen thickness and R is the radius of curvature. R was either 6 or 12 in, and the corresponding maximum strain in specimens $\frac{1}{16}$ in thick was about 0·55 and 0·25%.

Table 2 shows that at 20° C the life of many laminates was reduced by 5–10 times when they were subjected to such tensional strain on the surface exposed to the discharges. With the epoxy laminate E5, however, the discharge resistance was not significantly reduced by 0·25% strain at 20° C, while at 120° C, two samples of E5 under 0·55% strain showed longer life than unstrained samples, though presumably they would also have shown a longer life in the unstrained condition.

Table 2

THE EFFECT OF MECHANICAL STRAIN ON THE RESISTANCE OF GLASS LAMINATES TO SURFACE DISCHARGES

Impregnating resin	Code	Thickness	Stress	Life at 50 c/s*			
		Samples subject to a strain of		0	0·25%	0·37%	0·55%
Tests at 20° C		mm	kV/cm r.m.s.	hours	hours	hours	hours
Silicone	S3b	1·6	62	>2 600	265	—	—
	S8a	1·1	110	400	—	90 ± 30	—
Epoxy	E5a	1·5	62	600 ± 70	500 ± 80	—	—
	E5a	1·5	50	1 100 ± 230	1 200 ± 220	—	950 ± 160
	E5a	3·0	39	1 870 ± 100	—	—	850 ± 120
	E7f	2·2	56	1 150 ± 200	—	575 ± 50	—
	E8g	3·0	40	950 ± 100	—	—	500 ± 150
Polyester	Y1a	1·75	65	220 ± 20	20, 186	—	—
Melamine	M3a	1·6	75	33 ± 7	5	—	—
	M1b	1·6	76	—	—	—	10, 17†
	M1b	1·6	76	—	—	—	116‡
Tests at 120° C							
Silicone	S8a	1·1	110	400	—	62	—
Epoxy	E5a	1·5	80	140	—	—	210
	E5a	1·5	55	277, 682	—	—	625, 645

* The majority of tests were made at 500 c/s and the values of life at 50 c/s were calculated as 10 times the life at 500 c/s.
† Discharges on surface under tension.
‡ Discharges on surface under compression.

Table 3

RESISTANCE OF PHENOLIC-BONDED PAPER AND NYLON FABRIC LAMINATES TO SURFACE DISCHARGES, SHOWING THE EFFECTS OF TEMPERATURE AND MECHANICAL STRAIN

Material	Code	Thickness	Electric stress	Life at 50 c/s			
				No mechanical strain		At 20° C under strain of	
				20° C	105° C	0·25%	0·5%
		mm	kV/cm r.m.s.	hours	hours	hours	hours
Nylon fabric laminate..	P4a	3·25	45	>400	—	—	42
			37	550*	94	—	—
Paper laminates (electrical grade)	P5a	3·3	45	400	—	—	18 ± 2
			37	1 750 ± 250	114, 750	—	—
	P6	1·6	87	250 ± 30	—	—	—
			75	500 ± 50	—	—	—
	P7a	1·6	84	92 ± 15	<0·1†	10 ± 5	—
			76	125 ± 25	—	—	14 ± 4§
			76	125 ± 25	—	—	116‡
			65	161	0·2 ± 0·1†	—	95 ± 10
			53	—	74 ± 21	—	—

* These tests were made at 500 c/s, and the results are multiplied by 10. All other tests were made at 50 c/s to avoid possible thermal breakdown.
† At 105° C, cumulative heating by discharges causes very rapid failure if the stress exceeds 60 kV/cm r.m.s.
‡ Discharges on surface under compression.
§ Discharges on surface under tension.

Silicone-glass samples suffered considerable decrease in discharge resistance both at 20 and 120° C when subject to tensional strain.

Preliminary tests on melamine M2 indicate that compressive strain of the exposed surface has no adverse effect on the resistance to discharges. Similar insensitivity to compressive strain was shown by phenolic-paper laminate (s.r.b.p.) P7a, recorded in Table 3.

(3.3) Discharge Resistance of Phenolic-Bonded Paper and Nylon Laminates

Tests on unstrained samples were made using electrode system (*a*), and the effect of mechanical strain was examined using electrode system (*b*). The results, summarized in Fig. 2 and Table 3, indicate that:

(*a*) 'Electrical grade' s.r.b.p. obtained from different suppliers may differ appreciably in resistance to discharges (cf. samples P6 and P7).

(*b*) The discharge resistance of s.r.b.p. and phenolic-nylon laminates is greatly reduced at a temperature of 105° C. This occurs because the permittivity, loss angle and surface conductivity of these materials increase rapidly with rising temperature. Increased permittivity reduces the discharge inception voltage, while increased surface conductivity raises the energy of the discharges. Local heating by discharges may cause thermal instability at a comparatively low stress.

(*c*) If the stress is insufficient to cause thermal instability, deterioration takes the form of erosion around the centre electrode, and breakdown occurs at one of the eroded grooves [at B in Fig. 6(*a*)]. This Figure also shows peripheral cracks, about 1·5 cm from the edge of the high-voltage electrodes. These are formed by discharges of the kind shown in Figs. 6(*c*) and (*d*). A preliminary investigation of the conditions under which such discharges occur is in progress.

(*d*) The discharge resistance of s.r.b.p. and phenolic-nylon laminate is greatly reduced by mechanical tension. Although no surface cracks were evident when the material was first strained, cracks developed after a few hours' exposure to discharges and precipitated breakdown as shown by Fig. 6(*b*).

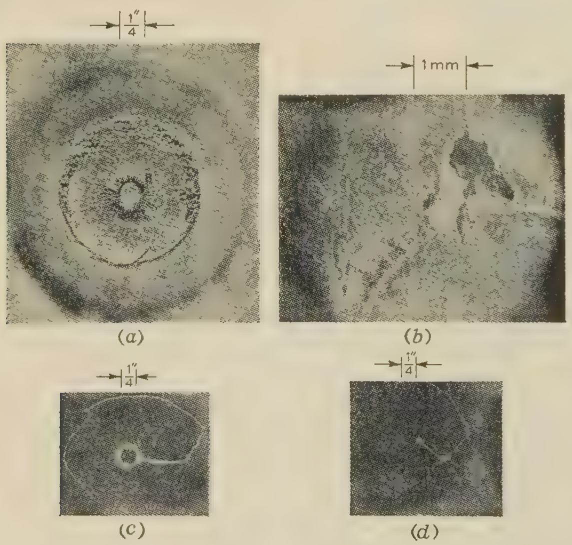

Fig. 6.—(*a*) Erosion and peripheral cracks caused by surface discharges in 1·6 mm s.r.b.p. sheet tested after 240 hours at 87 kV/cm r.m.s. 50 c/s at 20° C.

(*b*) Breakdown of 1·6 mm-thick s.r.b.p. sheet subjected to 0·5% tensional strain, after 16 hours at 46 kV/cm r.m.s., 50 c/s, at 20° C.

Surface cracks developed during the period of test and precipitated breakdown.

(*c*) and (*d*) Discharges in air on the surface of a Perspex sheet subjected to an electric stress of 100 kV/cm peak between a 6·35 mm-diameter rod h.v. electrode and a 15 cm-diameter plane l.v. electrode.

The exposure was for $\frac{1}{30}$ sec.

(3.3.1) Effect of Coating the Surface of Phenolic-Bonded Paper Laminate (S.R.B.P.) with an Anti-Tracking Varnish.

Previous investigations showed that the discharge resistance of moulded nylon-filled phenolformaldehyde resin was much greater for specimens containing moulded cavities than for similar specimens containing a machined cavity of equal size.[8] Apparently a surface layer of resin provides initial resistance to erosion. It seemed possible that a coat of an anti-tracking varnish on the surface of s.r.b.p. laminates would similarly increase their resistance to discharges.

Table 4 shows that the Admiralty anti-tracking phenolic

Table 4

THE EFFECT OF ANTI-TRACKING VARNISHES ON THE RESISTANCE OF PHENOLIC-RESIN-BONDED PAPER (S.R.B.P.) LAMINATES TO SURFACE DISCHARGES

Surface varnish	Comparative tracking index*	Life of specimens at 50 c/s			
		85 kV/cm r.m.s. and 20° C		53 kV/cm r.m.s. and 105° C	
		Average	Minimum	Average	Minimum
	volts	hours	hours	hours	hours
Unvarnished . .	100	92 ± 15	77	74 ± 21	40
V130 phenolic (red)	300	108 ± 22	87	61 ± 19	40
Alkyd . .	265	150 ± 70	84	92 ± 17	72

* The 'comparative tracking index' is the voltage required to cause tracking between chisel-edged electrodes, 4 mm apart on the surface of insulation, when 50 drops of 0·1% NH_4Cl solution are applied at 30 sec intervals.[4] The comparative tracking index depends on the orientation of these laminates. The minimum values are shown here. If the samples were rotated through 90° the comparative tracking index was about 20 volts greater in each case.

varnish, V130, had no significant effect either at 20 or 105° C. A coat of alkyd varnish, however, increased the average life of specimens by 60% at 20° C and by 30% at 105° C. The minimum life at 20° C was almost unaffected by the application of this alkyd varnish, but at 105° C, the minimum life was 75% greater than for the unvarnished laminate.

(3.4) The Discharge Resistance of Sheet Polymers

Fig. 2 shows that at 20° C polymethylmethacrylate (p.m.m.) and rigid polyvinyl chloride (p.v.c.) have approximately the same resistance to discharges as E5 epoxy-glass laminates, but a linear polythene R3 has considerably greater resistance. The discharge resistance of R3 and p.m.m. is little reduced by raising the temperature to 80–90° C, but rigid p.v.c. is very much less resistant to discharges than at room temperature. At stresses exceeding 60 kV/cm almost immediate thermal failure occurs if rigid p.v.c. is subjected to discharges at 500 c/s when the ambient temperature is 90° C.

Silicone rubber has outstanding resistance to discharges at temperatures up to 150° C. At 20° C the discharge resistance of specimen S152, which contains iron oxide in addition to silica filler, is lower than that of silicone rubber S80, which has only silica filler. At 150° C the S80 material hardened after exposure to discharges at 90 kV/cm for 1 000 hours, and cracked when folded, whereas S152 remained flexible after the same test.

(3.5) Discharge Resistance of Micanite

Preliminary tests have been made on 'moulding plate' and 'segment plate' micanites, bonded with shellac finish, and on a 'heat resisting' micanite bonded with an inorganic agent. All these micanites had much greater resistance to discharges than

any other material which has been tested. In tests on 0·6 mm-thick plates at 20° C and 50 c/s, the life of all micanite samples exceeded one year at 100 kV/cm (r.m.s.); at 160 kV/cm the minimum life of the heat-resisting micanite was one month, and that of the shellac-bonded micanite two months. At 250 kV/cm and 120° C, the heat-resisting micanite failed in 10–30 hours, and the shellac-bonded micanite in 15–500 hours.

(4) TIME-TO-BREAKDOWN TESTS ON THIN-FILM MATERIALS

Polymer films between 10 and 200 microns thick were tested using each of the electrode systems listed in Section 2. All methods gave the same general classification of materials, but the scatter in results was greater and discrimination between materials more difficult using electrode system (*a*) than with the other methods. This probably arose from mechanical damage to such thin films under the weight of the rod, or to difficulty in keeping the films uniformly in contact with a plane. The majority of tests were therefore made with a small air-gap between the rod and the specimen, which was lightly stretched over the cylindrical plate of electrode system (*c*). Some tests were also made with the crossed-cylinder system (*d*).

An air-gap of 75 ± 50 microns was found suitable, and the corresponding spacings between the rod and low-voltage electrode, for films of various thickness, are given in Table 5. The 50-micron tolerance on the gap permits considerable variation in the thickness of the films without resetting the gap and yet does not appreciably affect the scatter in results.

Comparison of results on materials common to Fig. 7 [using electrode system (*c*)] and Fig. 9 [system (*d*)] shows that system (*c*) reveals a more rapid variation of life with stress than (*d*). Otherwise the two systems give the same classification of materials and a similar scatter in results. The information in Section 4.2 derived from tests with system (*d*) therefore supplements the results in Section 4.1 obtained with (*c*).

For a standard test method, the rod and curved-plate system (*c*) is recommended since it is more easily assembled than the crossed cylinders and moreover is identical (except for the air-gap) with system (*b*) which is used to examine the effect of mechanical strain.

When tests are made with an electrode system involving an air-gap, the stress is calculated as the quotient of the applied voltage and the specimen thickness, t, measured near the point of failure. The thickness of the air-gap, t', between the rod electrode and the specimen is neglected, so that the results correspond to the practical case of discharges occurring in short air-gaps around the edge of an electrode in contact with the specimen. Similarly the discharge inception stress E_i is defined as V_i/t. The justification for neglecting t' is discussed in Section 9.4.

Table 5

SEPARATION BETWEEN ROD AND CYLINDER ELECTRODES REQUIRED FOR TESTING THIN SPECIMENS.

Material thickness	Electrode separation	Breakdown voltage of air-gap between rod and cylinder electrodes
microns	microns	kV peak
<30	80 ± 25	0·75 ± 0·10
30 – 60	100 ± 25	1·0 ± 0·1
60 – 100	150 ± 25	1·2 ± 0·1
100 – 150	200 ± 25	1·45 ± 0·1
150 – 200	250 ± 25	1·65 ± 0·1
200 – 250	300 ± 25	1·90 ± 0·1

The corresponding breakdown voltage of each gap is shown so that preliminary adjustment using a feeler gauge can be checked.

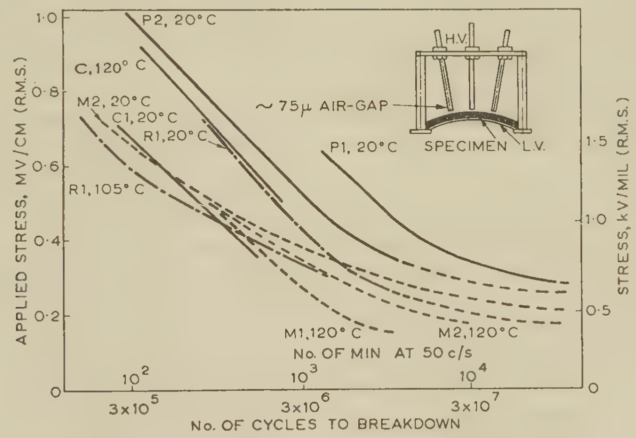

Fig. 7.—Effect of stress and temperature on the discharge resistance of thin sheet insulation.

The curves show the mean life of specimens tested in dry air between 6·35 mm-diameter h.v. rod electrodes and an l.v. electrode of 6 in radius of curvature.

Code	Material	Thickness	Temperature	No. of tests	E_i	Life at 0·4 MV/cm: 40 ± 10μ samples	Life at 0·4 MV/cm: 70 ± 10μ samples	Maximum coefficient of variation
		microns	deg C		MV/cm r.m.s.	min	min	
P1	Polythene (Grade 2)	65 ± 5	20	17	0·18 ± 0·03	—	5 000 ± 1 500	7·5
P2		35 ± 5	20	60	0·25 ± 0·05	2 000 ± 800	—	18
C	Polycarbonate Makrafol	41 ± 1	20	12	0·25 ± 0·02	1 200 ± 50	—	6
			120	12	—	370 ± 70	—	8
R1	Polythene (linear)	45 ± 7	20	42	0·16 ± 0·04	1 150 ± 400	—	18
			105	24	—	520 ± 120	—	10
M1	Melinex	75 ± 5	120	14	—	—	500 ± 150	14
			20	26	0·20 ± 0·05	800 ± 250	—	10
M2		40 ± 5	120	16	—	650 ± 80	—	7·5

(4.1) Time-to-Breakdown Tests using Rod and Curved-Plate Electrodes

(4.1.1) Tests with Alternating Voltage.

Electrode system (*c*) was used to compare the discharge resistance of polymer films between 30 and 80 microns thick. With these thin films, 16 tests could be made simultaneously, with the h.v. electrodes 2 in apart, without discharges at one electrode affecting the conditions at any other. Preliminary tests showed that voltage surges, caused by failure at one electrode, have no significant effect on the life at adjacent electrodes; accordingly, all the rods were connected together and to the high-voltage supply.

The discharge inception voltage, V_i, was determined at each electrode before life tests were begun, and an attempt was made to correlate the subsequent lives with values of V_i in case its variation should cause significant changes in life. Provided that the scatter of V_i did not exceed $\pm 20\%$ there was no obvious correlation when the test voltage was between $1 \cdot 5$ and 5 times the average value of V_i for the group.

The curves in Fig. 7 show the variation with applied stress of the mean life (in cycles) for films of Melinex (polyethylene terephthalate), Makrafol (polycarbonate) and for a low-density and a linear polythene. Tests at 50 and 500 c/s gave comparable results (i.e. similar life in total cycles) for Melinex, Makrafol and low-density polythene (melt index 2), provided that dry air was circulated over the surfaces under test. The curves for these materials in Fig. 7 show the average life calculated from the joint results of tests at both frequencies. With a linear polythene (melt index 5) the life at 500 c/s appeared to be 3–5 times longer than at 50 c/s, and the curves in Fig. 7 for this material are based on the 50 c/s results only. Subsequent tests indicated that this experimental film was very variable in quality although there was no visual inhomogeneity. The long lives at 500 c/s might therefore have been obtained from superior samples compared with those tested at 50 c/s.

Fig. 7 shows that the relative life of the different materials varies with both the applied stress and the thickness of the material. Nevertheless the materials can be satisfactorily classified in order of resistance to discharges, as shown in the caption. Many other materials were tested with electrode system (*d*), and a classification for these is given in Section 4.2.

(4.1.2) Tests with Sinusoidal Voltage Pulses.

Tests with 50 c/s sinusoidal half-wave voltage pulses were made on 35-micron thick sheets of polythene (melt index 2) using electrode system (*c*). At stresses exceeding 2 MV/cm the life with positive pulses is about four times that with equal negative pulses applied to the rod electrodes. The life with negative pulses is also some three times greater than that predicted by extrapolating the results of 50 c/s a.c. tests, as shown in Fig. 8.

These results are similar to those of Nail[12] for 2-mil polythene tested between rod and plane electrodes, although he found a greater difference, e.g. an average life of 72 min with +10 kV pulses and $2 \cdot 5$ min with −10 kV pulses.

(4.2) Time-to-Breakdown Tests using Crossed-Cylinder Electrodes

The electrode system (*d*) is described in Section 2 and illustrated in Fig. 9. The separation between the h.v. and l.v. electrodes was adjustable, and tests on thin specimens were made with the electrode separations shown in Table 5, so that there was always an air-gap between each rod electrode and the surface of the specimen. Dry air was passed over the specimens during test, for the reasons discussed in Section 1.

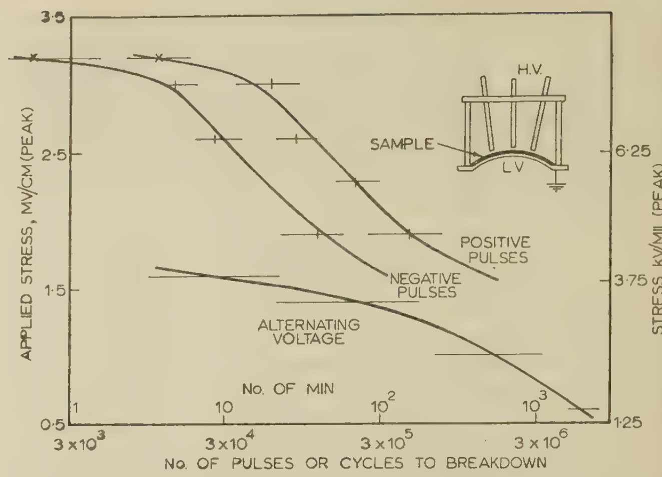

Fig. 8.—Effect of stress and polarity on the life of 35-micron polythene sheet subjected to discharges between rod and cylinder electrodes.

Fig. 9 shows the results of tests using these electrodes for several polymer films of about 40 microns thickness. Comparison with Fig. 7 shows that the classification of materials and the scatter in results is similar to that obtained with the rod and curved-plate electrodes.

Fig. 9 shows two curves R_1 and R_2 for linear polythene. These materials were nominally similar and were supplied by the same manufacturer. Possibly material R_1 was in a more strained condition and thus had lower resistance to discharges at 20° C than R_2. The relation of the two materials was reversed, however, at stresses exceeding $0 \cdot 9$ MV/cm at 20° C and $0 \cdot 5$ MV/cm at 105° C. Further investigation is evidently required.

Fig. 9 shows also that at 20° C the discharge resistance of the linear polythene was intermediate between that of low-density polythenes with melt indices 2 and 7, whereas it might be expected that the material with greatest thermal stability would also have the greatest resistance to discharges. Possibly neither R_1 nor R_2 represents the optimum quality of linear polythene, or it may be that this material is inherently more liable than low-density polythene to embrittlement under the action of discharges.

The poor discharge resistance of the p.t.f.e. films confirms Dakin's investigations.[13] Tests on thicker films showed that p.t.f.e. film veneered from a sintered block has even lower resistance to discharges than films cast from dispersion, as shown in Table 6. The poor resistance to discharges of the veneered film is undoubtedly due to its inhomogeneous structure, but the films from dispersion are more homogeneous, and Dakin suggests that failure by discharges may be facilitated by the high electron affinity of fluorine.

The tests showed that the relative times to failure for different materials vary with the applied stress, the ambient temperature, and the thickness of the material. Nevertheless the results summarized in Table 6 give a satisfactory classification in respect of resistance to discharges.

(5) GENERAL DISCUSSION

The discharge-resistance tests proposed in the paper have several advantages. The test procedure is simple and the results given in Sections 3 and 4 clearly differentiate materials in respect of their resistance to breakdown by discharges under the particular test conditions, and also demonstrate that mechanical strain and increase of ambient temperature greatly reduce the discharge resistance of most materials. The test conditions may,

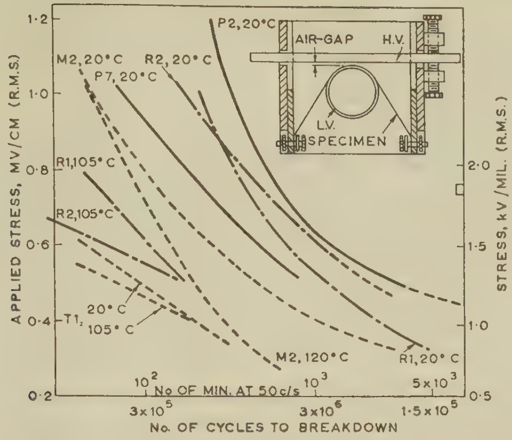

Fig. 9.—Effect of stress and temperature on the discharge resistance of thin sheet insulation.

The curves show the mean lives of specimens tested in dry air between the crossed-cylinder electrodes. Electrode spacing: 100 microns.

Code	Material	Thickness	Temperature	No. of tests	E_i	Life at 0·6 MV/cm 50 c/s	Maximum coefficient of variation
		microns	deg C		MV/cm r.m.s.	min	%
P2	Polythene Grade 2	32 ± 5	20	20	0·3 ± 0·05	1 400 ± 150	6
P7	Polythene Grade 7	41	20	40	0·19 ± 0·03	500 ± 200	18
R1	Linear polythene 1	44 ± 5	20	20	0·25 ± 0·05	700 ± 300	18
			105	10	—	110 ± 25	12
R2	Linear polythene 2	44 ± 8	20	20	0·25 ± 0·05	1 100 ± 350	20
			105	10	—	60 ± 35	20
M2	Melinex	39 ± 3	20	36	0·3 ± 0·05	250 ± 70	12
		32 ± 2	120	10	—	150 ± 50	15
T1	P.T.F.E. from dispersion	32 ± 2	20	15	0·21 ± 0·03	45 ± 17	25
			105	10	—	~25 ± 15	25

Table 6

THE COMPARATIVE DISCHARGE RESISTANCE (C.D.R.) OF THIN SHEET INSULATING MATERIALS. COMPARISON OF THE TIME TO BREAKDOWN OF VARIOUS POLYMER FILMS TESTED BETWEEN CROSSED CYLINDER ELECTRODES

Sample thickness, microns		38 + 8		68 + 15		185 + 25	
Applied stress, kV/cm r.m.s.		600		600		300	
Material and thermal classification	Ambient temp.	Time*	C.D.R.†	Time	C.D.R.	Time	C.D.R.
	deg C	min	%	min	%	min	%
Normal polythene Grade 2 (80° C)	20	1 400 ± 150	100	—	—	—	—
Linear polythene R1 (A, 105°C)	20	1 100 ± 350	78	2 000 ± 300	100	—	—
Linear polythene R2 (A, 105° C)	105	60 ± 35	4·2	—	—	—	—
Normal polythene Grade 7 (70° C)	20	500 ± 200	36	1 300 ± 250	65	15 000	100
Polyethylene terephthalate (a) (Melinex) (E, 120° C)	20	250 ± 70	18	—	—		
	120	150 ± 50	11	—	—		
(b) (Mylar) (E, 120° C)	20	60	4·2	100 ± 40	5		
	120	—	—	100 ± 40	5		
Cellulose acetate (A, 105° C)	20	—	—	—	—	1 000	6
	105	—	—	—	—	150	1
Polytetrafluoroethylene (C > 180° C)	20	45 ± 17	3·2	20 ± 10	1		
(a) Electrical Grade	140	20 ± 15	1·5	5 ± 3	0·25		
(b) Veneered from block	20	—	—	12 ± 9	0·6		

*The time quoted is the number of minutes to breakdown when tested at 50 c/s.
† The c.d.r. is quoted as a percentage of the longest life in the column under consideration.

however, appear remote from those pertaining to practical insulation, where failure is often caused by internal discharges which are usually smaller than surface discharges. While it might be preferable to make tests with internal discharges, this is more difficult, and in many materials internal discharges are intermittently short-circuited by semiconducting films formed on the cavity surfaces.[8, 15] It would then be difficult to assess life under conditions of intermittent loading. This difficulty is obviated by tests with surface discharges, provided that dry air is passed continuously over the specimen. As the average time to breakdown for materials is then a minimum, the test measures the resistance of materials to discharges under the most severe conditions, as, for example, in insulation operating at such high temperatures that moisture is eliminated. This last sentence must not be taken to imply that it is desirable to operate insulation in damp conditions, since these may give rise to other forms of deterioration, e.g. electrochemical effects, increased dielectric losses causing thermal failure, and surface tracking.

Some materials, for example silicone rubber, have excellent resistance to all forms of deterioration, while others, e.g. some grades of phenolic-resin-bonded paper, have poor quality in every case, but such uniform behaviour must not be assumed; for example, p.t.f.e. has poor resistance to penetration by discharges, but is unaffected by other mechanisms. There is an evident need for standard tests to assess the resistance of new materials to all these forms of deterioration.

In America, discharge resistance tests have been made using cylindrical high-voltage electrodes of either $\frac{1}{4}$in diameter,[13] $\frac{1}{2}$in diameter[10] or 2in diameter,[15] mounted perpendicularly to, and in contact with, specimens which are usually mounted on a larger low-voltage electrode, although some tests have been made using high-voltage and low-voltage electrodes of equal diameter.[15] As it is the area around and not under the electrode which is subject to test, the smaller h.v. electrodes are preferable, since, with these, a larger number of tests can be made on a given area of material. The l.v. electrode should be much larger than the h.v. one, otherwise the electrodes must be set accurately coaxial to avoid changes in the stress distribution in successive tests on a material.

Rollinson has suggested[16] that needle points should be used instead of $\frac{1}{4}$in-diameter rods, but it is more difficult to standardize and to maintain the shape of a point electrode. Discharge detection tests show that, except at the discharge inception voltage, there is little difference in the magnitude of discharges around point and rod electrodes.

It would be desirable to know the effect of the discharge magnitude on the rate of deterioration, but it must be remembered that the discharge magnitude depends on the capacitance which is discharged, i.e. on the nature and pressure of the gas and the length of the discharge gap, and on the thickness, permittivity and surface conduction of the insulation. Also it often varies with the time of voltage application, so that it is more realistic to compare the lives of materials of a given thickness at a defined stress and under specified ambient conditions, rather than attempt to expose materials to discharges of specified magnitude. The deleterious effects of heat and mechanical strain on the discharge resistance of most insulating materials show that the electrical, thermal and mechanical properties of insulation cannot be considered in isolation. Ultimately the suitability of insulating materials must be assessed in conditions closely simulating those in service. In the case of moulding materials Parkman[17] proposes that discharge resistance tests should be made on 'flow cup' samples tested between rod and plate electrodes at the maximum temperature rating[18] for the material. The dimensions of these flow cups approximate to those of many components used in electronic equipment, and the cups are likely to be moulded in a state of strain which is comparable with that occurring in practical mouldings.

(6) CONCLUSIONS

Time-to-breakdown tests using the simple electrode systems described in Section 9 provide a reproducible classification for the discharge resistance of thin polymer films and for thick sheet and laminated insulating materials. Comparison should be made only between materials of similar thickness. It is essential to circulate dry air over specimens during test, in order to prevent the formation of semiconducting surface films. For many materials life tests at 20° C can be accelerated by raising the frequency to 500 c/s or 1 kc/s, but a higher frequency may cause local cumulative heating by the discharges and a shorter life in cycles than tests at 50 c/s. At higher temperatures there is a greater probability of breakdown by thermal instability, and frequency-accelerated tests are more liable to give misleading results. Thus, for any material, it is advisable first to confirm the approximate equivalence of life (in cycles) at 50 c/s and at the desired test frequency.

For research purposes the variation of life with applied stress should be determined for several thicknesses of a material at 20° C and at its maximum temperature rating. Differences between materials increase with rising ambient temperature, and proof tests can be made on a specific thickness at a defined high temperature and stress.

The results show that adequate bonding between the resin and the glass fabric is essential to obtain the optimum resistance to discharges in resin-glass laminates, and they also show that laminates with a coarse weave have less resistance to discharges than those with a fine weave. The application of mechanical tension reduces the discharge resistance of most laminates.

At room temperature a number of materials of quite different structure, e.g. polymethylmethacrylate (p.m.m.), epoxy-glass laminates, electrical grade phenolic-bonded paper boards (s.r.b.p.) and rigid polyvinylchloride (p.v.c.) show similar resistance to discharges over a considerable range of applied stress. At higher temperatures cumulative heating by discharges causes thermal breakdown in p.v.c. and s.r.b.p. at quite low stresses.

A few materials, e.g. silicone rubber and the best grade of silicone-glass laminate, have outstanding resistance to discharges up to 150° C, and 1·5 mm sheets of these materials can withstand discharges at 100 kV/cm for more than 1 000 hours at 50 c/s, but minor defects in the structure, caused for example by mechanical strain, greatly reduce their discharge resistance.

There is little correlation between the thermal classification of materials and their resistance to breakdown by discharges. Some materials with excellent thermal stability, e.g. silicone, rubber and the best silicone glass laminates (Class H, 180° C) also have excellent resistance to discharges but others, e.g. p.t.f.e. (Class C) and the less homogeneous silicone-glass laminates, have much less resistance to discharges than polythene or polymethylmethacrylate which can be used only up to 70° C.

Under unidirectional voltage pulses the life of thin films of polythene is considerably greater at a given peak stress than at the same alternating peak stress.

Further tests with unidirectional pulse voltages should be made to determine the effects of polarity on the discharge resistance of various thicknesses of different materials.

(7) ACKNOWLEDGMENTS

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(9) APPENDICES

(9.1) Time-to-Breakdown Tests on Sheet Insulation Exceeding 0·5 mm Thickness (without Mechanical Strain)

Stainless-steel rod electrodes of $\frac{1}{4}$ in diameter, with flat ends and square edges, are mounted perpendicularly above the specimen, which rests in contact with a plane low-voltage electrode, as shown in Fig. 3(*a*). The low-voltage electrode can be either brass or aluminium for tests at temperatures below 150° C, but stainless steel is necessary for tests at higher temperatures.

The edge margins and also the separation between the electrodes must be chosen in relation to the test voltage and the effective thickness of the specimen, t/ϵ. Fig. 10 indicates the

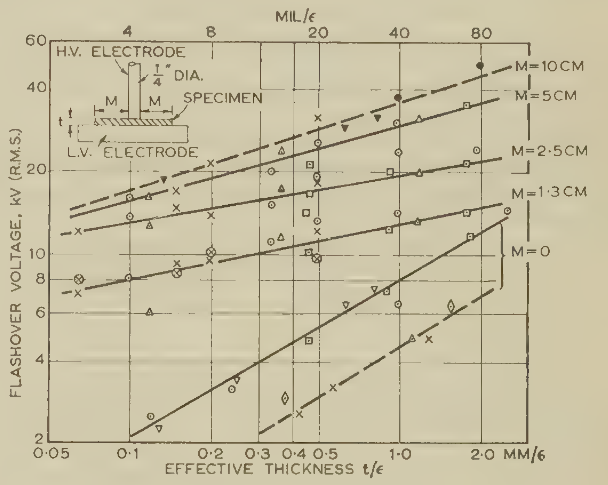


Fig. 10.—Variation of flashover voltage with thickness t, permittivity ϵ and width of margin m.

Tests were made at 20° C, 760 mm Hg pressure and 45% relative humidity.

Material	ϵ	Symbol	
		m = 0 to 5 cm	m = 10 cm
Polythene	2·3	×	—
S.R.B.P.	6·5	○	●
Silicone rubber ..	3·0	△	—
Mycalex	7·0	□	—
Glass	8·0	▽	▼
Perspex	≃4	◊	—

All tests were carried out at 50 c/s except those marked ⊗ for polythene tested at 500 c/s.

edge margins required to avoid flashover in tests at 20° C at voltages up to 40 kV r.m.s. The separation between electrodes should be at least double the edge margin. Greater margins are required at higher temperatures, because the electric strength of gases falls, and the permittivity of most materials increases with rise in temperature.

(9.1.1) Research Investigations.

It is desirable to determine the discharge inception voltage and the discharge magnitude at each electrode before life tests are commenced. Large variations in these quantities at the different electrodes indicate an inhomogeneous material. Life tests should be made at voltages up to six times the average value of V_i, at 20° C and at the maximum operating temperature.[17] Dry air should be circulated over samples during test.

To simulate certain practical conditions, copper or brass rod electrodes could be used instead of stainless steel, and tests made at a controlled humidity. Under such conditions it may be more relevant to determine the margin necessary to prevent flashover after a period of test, or to measure changes in the loss angle or

insulation resistance of the specimen, than to determine the time to breakdown by erosion.

Provided that dry air is circulated over specimens, life tests at 20° C on most materials can be accelerated by raising the frequency to 500 c/s or 1 kc/s, but this is not possible for tests in static air or humid conditions because the rate of formation of semiconducting films increases with frequency more rapidly than the rate of erosion, so that the life in cycles is much greater at the higher frequency. It may also be misleading to accelerate tests at higher temperatures by raising the frequency. It is advisable to confirm the approximate equivalence in life (in cycles) at 50 c/s and at the desired test frequency before extensive tests are commenced.

If dry air is circulated, the life of a specimen, in cycles of applied voltage, is little affected by periodic interruption of the test. It is therefore permissible for failure at one electrode to trip the supply voltage to all the tests, and also to stop a clock recording the duration of test.

The life of a specimen in moist or static air may be considerably affected by interrupting the test. A fuse is then required with each rod electrode and the life at each electrode is recorded separately.

The thickness of samples should be determined near the points of failure. The average life and the mean deviation in life at each stress can then be calculated.

(9.1.2) Proof Tests.

It is sufficient to determine the average and the mean deviation in the life of five samples of a material at a given stress (e.g. 50 kV/cm r.m.s. for materials between 1 and 2 mm thick) at 20° C and at the maximum thermal rating temperature for the material. The quality of the material can then be estimated by comparison with life/stress characteristics determined in research investigations.

(9.2) Time-to-Breakdown Tests to Determine the Effect of Mechanical Strain on the Discharge Resistance of Sheet Insulation

If a specimen of thickness t is bent over the cylindrical low-voltage electrode of the apparatus shown in Fig. 3(b), the upper surface is subject to a tensional strain of $t/2R$, where R is the radius of curvature of the l.v. electrode. Specimens $\frac{1}{16}$ in thick are thus subject to about 0·5% strain with $R = 6$ in and about 0·25% strain with R = 12 in.

The stainless-steel h.v. rod electrodes are mounted perpendicular to and just in contact with the surface of the specimen. The separation between the electrodes, the edge margins and the test frequency should be chosen as in Section 9.1, and dry air should be passed over samples during test.

The test voltage and temperature should be chosen in relation to the results of life tests on unstrained materials.

(9.3) Time-to-Breakdown Tests on Sheet Insulation Less than 0·5 mm Thick

Stainless-steel rod electrodes of $\frac{1}{4}$ in diameter, with flat ends and square edges, are mounted perpendicularly above the specimen, which is stretched lightly over the low-voltage electrode. This is a segment of a brass cylinder of 6 in radius of curvature as shown in Fig. 3(b).

The upper end of each rod electrode is threaded (40 turns/in), so that a small air-gap can be readily introduced between the end of each rod and the l.v. electrode by rotating the rod in the brass clamps mounted in the Mycalex supporting sheet. The position of the rod can be locked by tightening a nut on the rod against the upper face of the brass clamp. The gap between the electrodes should be 75 ± 50 microns greater than the sample thickness.

The electrode gaps should be adjusted before the specimen is mounted on the l.v. electrode. The gap width can be set with a feeler gauge, but it is advisable to check it by measuring the breakdown voltage of the air between each rod and the l.v. electrode. The breakdown voltage, corresponding to the air-gaps required for films of a given thickness, are shown in Table 5. A 100-kilohm resistor should be used in series with the test gap, when this breakdown test is made, to limit the discharge magnitude and prevent erosion of the electrodes.

The separation between adjacent rod electrodes and the margin between the rods and the edges of the specimen must be chosen in relation to the test voltage and the thickness and permittivity of the specimen. Tests on specimens between 20 and 100 microns

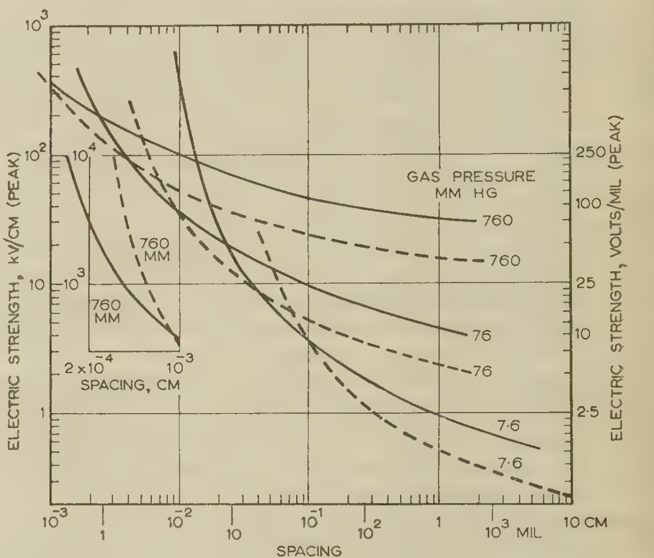


Fig. 11.—Variation with electrode separation of electric strength of air and hydrogen in a uniform field at 20° C, and 760, 76 and 7·6 mm Hg pressure.

—— Air. ---- Hydrogen.

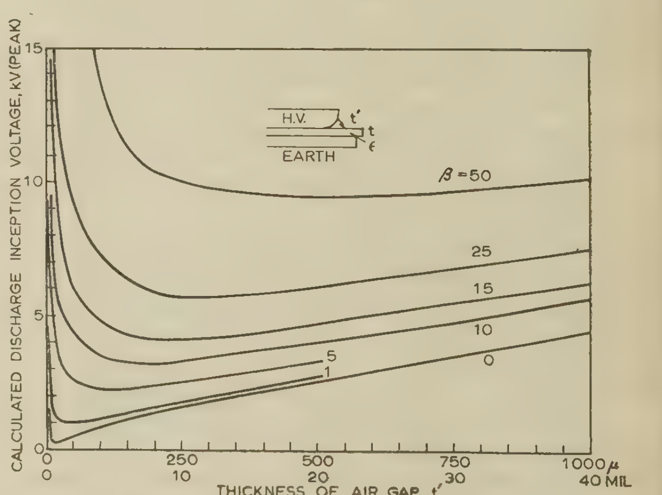


Fig. 12.—Variation of discharge inception voltage at the edge of electrodes in air at 20° C and atmospheric pressure with the thickness of the air-gap, for sheets of insulation of varying thickness and permittivity.

$\beta = t/\epsilon$ mil. $V_i = E_G(t' + \beta) = V_G(1 + t/\epsilon t')$.

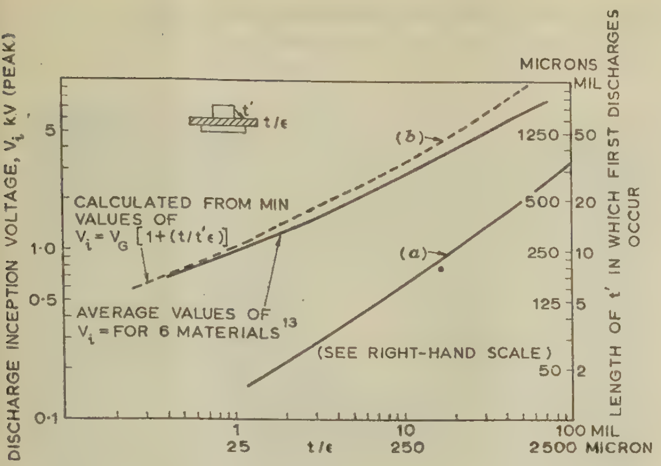


Fig. 13.—Curve (*a*) shows the length of air-gap corresponding to minimum discharge inception voltage [shown in curve (*b*)] at the edge of an electrode on the insulation of thickness t and permittivity ϵ in air at atmospheric pressure and 20° C.

Curves (*a*) and (*b*) are derived from Fig. 12.

thick may be made with the electrodes in all the positions shown in Fig. 3, but greater electrode separation may be required for tests on thicker specimens. Suitable edge margins for tests at higher voltages are indicated by the curves in Fig. 10.

After adjusting the electrode gaps and inserting the specimen, it is advisable to check the discharge inception voltage V_i, and the discharge magnitude at each high-voltage electrode. There is inevitably some scatter in the values of V_i, but it should not exceed $\pm 20\%$. Greater spread indicates considerable variations in the thickness and homogeneity of the sample and/or error in adjusting the electrode separation. A spread of less than $\pm 20\%$ does not appear to affect the spread in life-test results. The discharge magnitude is usually quite variable at V_i, but differences between test samples tend to decrease after a few minutes test, particularly at higher stresses.

Dry air should be circulated over specimens for the reasons given in Section 9.1.

(9.3.1) **Research Investigations.**

Specimens should be tested at voltages up to $5V_i$. After failure has occurred at all the electrodes, the thickness of the material near each breakdown site should be determined using a dial micrometer. The average life and mean deviation in life at different stresses can then be calculated, and the characteristic variation of life with applied stress plotted as shown in Fig. 9.

(9.3.2) **Proof Tests.**

It is sufficient to determine the average life and mean deviation for ten tests at a given stress and temperature, e.g. at 400 kV/cm r.m.s. for a material between 20 and 200 microns thick at 20° C, or at the maximum operating temperature for the material. The stress should be calculated from the nominal thickness of such materials, and the life at this nominal stress compared with results of research tests on similar material.

(9.4) **Factors Determining the Inception Voltage and Location of Surface Discharges**

Discharges at the edges of conductors adjacent to sheet insulation begin at a voltage V_i, determined by the permittivity and thickness t of the sheet, by the shape and symmetry of the electrodes and by the electric strength E_G and permittivity ϵ_b of the ambient medium. If the medium is air, E_G varies with the pressure and length of the discharging gaps, as shown in Fig. 11. For insulation tested in air between unequal electrodes the inception voltage, V_i, for discharges of length t' at the edge of the high-voltage electrodes can be derived from the minimum values[19] of the expression $V_i = E_G(t' + t/\epsilon)$.

Fig. 12 shows that even with very thin insulation ($t/\epsilon \simeq 1$ mil) the minimum value of V_i occurs in a gap exceeding 1 mil thickness, and the first discharges occur in gaps of greater length as t/ϵ increases. Curves (*a*) and (*b*) in Fig. 13 are derived from Fig. 12 and predict how V_i and the corresponding value of t' should vary with t/ϵ. Curve (*b*) shows quite good agreement with experimental results and approximates also to an empirical formula

$$V_i = k(t/\epsilon_T)^{1/2}(293/T)$$

where ϵ_T is the permittivity of the material at the test temperature T. If t is expressed in mils, V_i is given in kilovolts peak, with $k = 10^3$ for air at atmospheric pressure and 20° C.

Consideration of Fig. 12 shows that, if the applied voltage is raised only 25% above V_i, discharges can occur in much longer gaps than those corresponding to discharge inception. Thus a small gap (75 ± 50 micron) between the h.v. electrode and the surface of a thin specimen (as recommended in Section 9.3) has little effect on either the discharge inception voltage or the energy of discharges, and it is permissible to neglect t' in calculating the stress applied to the specimen.

DISCUSSION BEFORE THE INSTITUTION, 12TH MAY, 1960

Mr. P. R. Hartshorn: Very early in the paper, the author mentions that the results obtained should not be used for engineering design purposes. However, this does not in any way lessen the importance of the work, because one cannot visualize any way of tackling such a complex problem other than by research under idealized conditions. Despite this cautionary note on the part of the author, it is possible that the results may be of practical value, because of applications in which idealized conditions are approached.

However, in the majority of cases conditions are far from ideal, and there are other factors to be taken into account. Materials such as the author has tested have attractions for use in outdoor conditions, e.g. in railway electrification works. However, when one tries to use such materials under these conditions one soon finds that one has to think in terms of not kilovolts per centimetre but of volts per centimetre.

I suggest two other properties of the material which must be taken into account besides its inherent tracking propensity, and these are its resistance to water and chemical attack. Using the intrinsic values which the author has produced and considering them in relation to the two other factors, we may well have a fair indication of what is to be expected of the materials under actual service conditions.

A very good example is provided by p.t.f.e., which has a very low resistance to discharges. On the other hand, it has excellent resistance to water and is chemically inert, and appears to perform far better under outdoor conditions than any other materials, excluding only the vitreous materials, glass and porcelain.

Monsieur G. Leroy (*France*): It is a difficult task to improve methods for comparing the resistance of sheet insulation to breakdown by discharge because of the many parameters which are involved. In fact, it seems impossible to simulate all working conditions at the same time, and therefore we have to select

only a few. Clearly the choice of test conditions is of the utmost importance in obtaining results related to subsequent service experience. We must consider the matter from two distinct aspects according to the nature of the selected test conditions.

The first method, which is considered in the paper, is the analytical approach. The author attempts to study the action of each parameter separately. Therefore, he lays emphasis on the action of ambient temperature, applied stress, mechanical strain and thickness of the sheet under test. The paper gives us a better understanding of the many degradation mechanisms caused by discharges.

However, there is also the point of view of the user. When we consider the behaviour of materials under service conditions, we find that discharges occur in confined atmospheres. When air contains a normal percentage of humidity, the accumulation of nitric oxides formed by discharges forms a semiconducting film which may short-circuit the discharges and cause chemical degradations. This phenomenon modifies the magnitude of the discharges during test and increases the scatter of results. I therefore agree with the author's method when the purpose of the test is the analytical study of each parameter. However, it would be dangerous to draw any conclusion about the resistance of sheet insulation without considering the chemical action of degradation products. For instance, we carried out a series of tests on Samica sheets bonded with silicone resin, asphalt and natural resins. A comparison of the test results showed very large differences in the behaviour of these materials according to the nature of the bonds, which were more or less affected by chemical attack. In France experiments are being carried out using large-area electrodes to find the effect of acidity, loss of weight or insulation resistance.

The two methods are complementary and should be used together. After first concentrating on the second method, we are now starting extensive tests on the first.

Mr. K. H. Stark: The difficulty of assessing the discharge resistance of insulating materials lies in choosing a realistic model. The one proposed by the author has the virtue of simplicity and of giving reproducible results. Point electrodes, as proposed by Rawlinson, are more difficult to use than rod electrodes, and do not lead to greater reproducibility.

However, I am not so happy about the need for passing dry air over the test specimens, or how dry this air stream has to be. In Fig. A, the discharge magnitude is plotted as a function of the applied voltage for different test times, after being tested nearly under the proof test conditions (Section 9.1.2) proposed

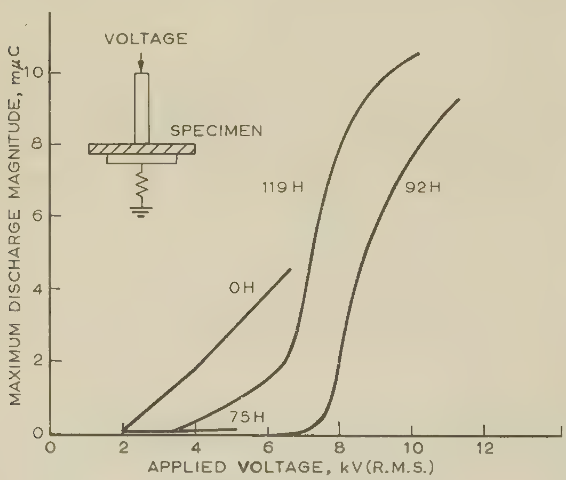
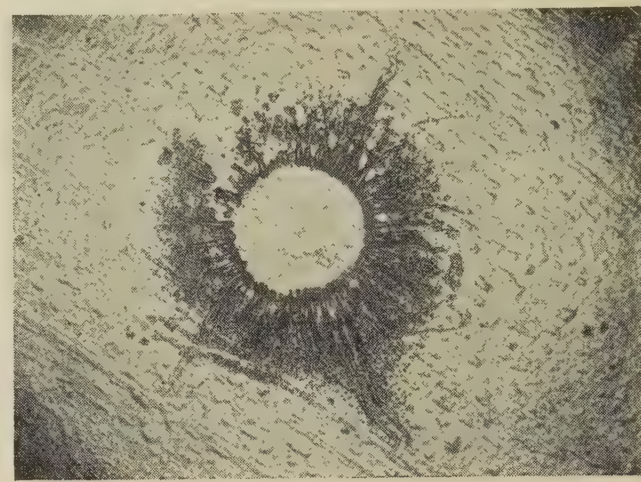
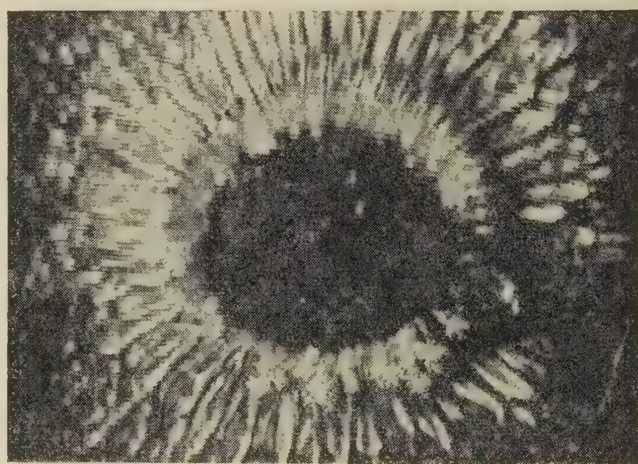


Fig. A.—Variation in discharge characteristics of $\frac{1}{16}$ in s.r.b.p. sheet with time of application for 10 kV(r.m.s.) 500 c/s.

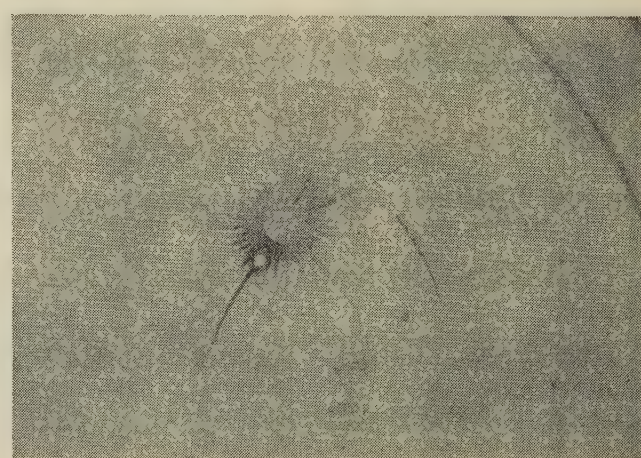
by the author. Though suppression of the discharges does occur at first, this is reversed after further testing, in this case after 119 hours. Also the discharge magnitude at the test voltage



(i)



(ii)



(iii)

Fig. B.—Discharge erosion pattern for epoxy-resin sheet tested between $\frac{1}{4}$ in-diameter rod and plane electrodes.

(i) Tested under conditions proposed by the author.
(ii) Thickness twice that proposed by the author, but the electric stress kept the same.
(iii) Thickness four times that proposed by the author, but the electric stress kept the same.

of 10 kV remains sensibly constant throughout the test. I have found that, for test specimens with reasonable access to the ambient air, passing dry air over the specimen of s.r.b.p. and epoxy-resin sheet does not alter the times to breakdown.

In the proof test for the discharge resistance of sheet materials (Section 9.1.2), tests at 50 kV/cm on a 1–2 mm thickness are suggested. I wonder whether variations with thickness can be ignored, as thicknesses greater than 2 mm are often used when the erosion mechanism may change. An illustration of this is given in Fig. B. Fig. B(i) shows the discharge pattern formed on an epoxy-resin sheet tested under the conditions proposed, when the normal erosion pattern is formed. For Fig. B(ii) the thickness has been doubled, but the electric stress is kept constant. The breakdown is now caused by a new mechanism which erodes dendritic channels. Doubling the thickness again, but still keeping the stress constant, gives yet another form of failure, as shown in Fig. B(iii). Only a few, deep radial grooves are formed together with some peripheral grooves.

Is it possible that the small effect of temperature on the discharge resistance of epoxy-glass laminates under strain arises from the resin being rubbery at a test temperature of 120° C, when the strains within the resin will be reduced? If mechanical tension lowers the discharge resistance of a material, what is the effect of compression?

Mme B. Fallou (*France*): We are using a test setting which appears to behave in quite a different manner, the electric discharges being uniformly distributed over the surface of the sample. The area of the sample is wide enough to allow a number of different measurements to be performed during its ageing. Various criteria might be used to define the material's ageing, and these are related to the mechanical, electrical or chemical properties of the samples. In this cell, plane electrodes are laid parallel to the sample, which is thus placed in an air-gap in one of the following arrangements:

(i) An insulating plate, an air-gap, the sample and an insulating plate.
(ii) An air-gap, the sample and an insulating plate.

Air circulates freely at the surface of the sample, with the cell placed inside a ventilated oven at the chosen temperature. The energy lost in the air-gap can be determined by calculation, and follows simple laws which have been checked with a Schering bridge. To keep the power loss constant, it is sufficient to maintain a constant voltage across the sample. The main conclusions which were drawn after a number of tests on various insulating materials can be summarized as follows:

The types of damage which are noticeable on an aged insulation film depend upon the number of charge impacts per half-period, their amplitude and the area covered by the discharge. Thus a number of small charges spreading over a large area result in a uniform potential at the surface of the sample, inducing nearly uniform wear. This is typical of the behaviour of cell No. 1, whose capacitance is more than ten times smaller than that of the sample. On the other hand, large charges spreading over small areas lead to concentrated fields at certain spots where 'break-through' might occur. However, concentration of the discharge at these points results in less average wear of the sample. This is the situation with cell No. 2, which is made of a single insulating plate of high capacitance, whose value approaches that of the sample. The average charge is nearly ten times greater than in cell No. 1.

The 'loss of weight' criterion is quite easy to apply in both cells, so long as no 'break-through' has taken place. Breakdown criterion cannot be fully applied in cell No. 1. In cell No. 2 new criteria, of the 'break-through kind', can be used; we observe an important increase in the total cell current. The sample is subjected to electric fields quite comparable with those

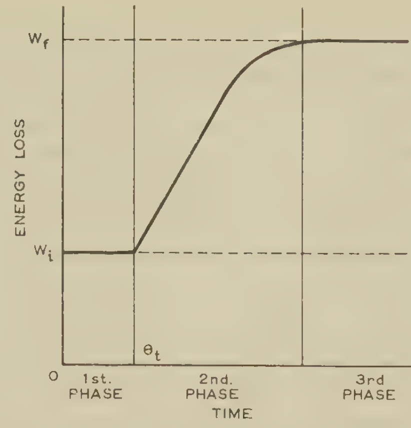


Fig. C.—Variation of energy loss with time.

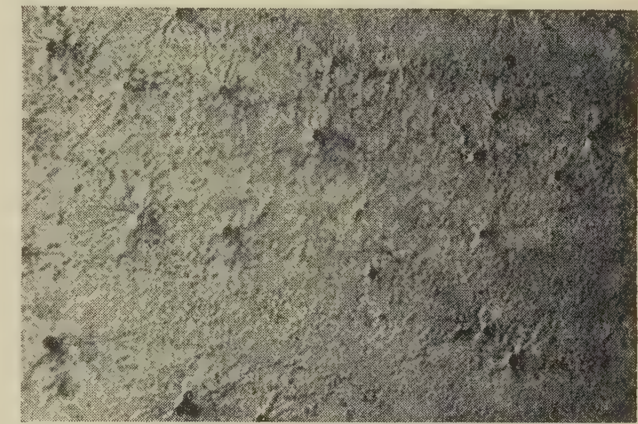


Fig. D.—Synthetic mica in cell No. 2.
The state of discharge and the numerous punctures can be seen.

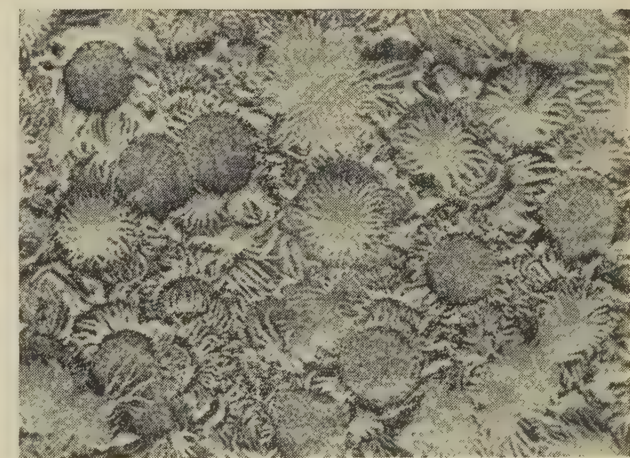


Fig. E.—A map of the charged regions of the sample in the first cell obtained by spraying the insulating sheet surface with selected powders.
It will be seen that the discharge is quite uniformly distributed over the surface.

in the paper. It appears that both methods might lead to the same classification of insulating materials if we consider that the test ends with the first breakdown in both cases.

The following features of cell No. 2 are of particular interest to users:

(i) Its plane symmetry allows a uniform spread of charge, whose amplitude is, in general, of the same average value.

Thus the regions where breakdown occurs will be determined not by the cell configuration but by the weak points of the sample, which are quite important in the case of heterogeneous insulation.

(ii) Its insulating plate, being of high electric strength, allows not a single discharge but a large number of discharges leading to breakdown to be 'quickly' recorded on the insulating material. The risk of taking accidental breakdowns into account is thus diminished, and the size and the map of the breakdown region are available for inspection.

Mr. J. S. Simons: In my company we are much concerned with the laboratory evaluation of insulation for large rotating machines. Functional tests are recognized as being of particular importance, and amongst them are corona-discharge erosion-resistance determinations. For this work needle-point electrodes similar to those advocated by Baker and Rawlinson have been used, but made from tungsten wire 0·06 in diameter with a sharp-ground 60° point. These needles have been found to be more corrosion resistant than gramophone needles. In service, atmospheric conditions change, and in our initial functional tests we did not control temperature and humidity, but accepted the normal ambient variations. There appears to be justification for controlling these variables where comparative results are required.

At present most insulation systems for machines are based on mica, and as the author points out this material is very resistant to discharges. In order to obtain breakdowns in a reasonable length of time (several months) we have found it necessary to increase both the applied stress and the supply frequency. Even so, with 0·060 in-thick bonded-mica insulation we have obtained a life equivalent to 20 years when the test conditions were three times the normal service stress at 1·5 kc/s. The latter frequency may be regarded as rather high for computing equivalent life at 50 c/s, but in the case of mica materials our tests have shown no evidence of substantial dielectric heating or other objectionable secondary effects. It seems desirable that, wherever possible, a number of tests should be carried out over a range of frequency, and the frequency/life relationship should be established before computing equivalent lives at lower frequencies.

The author has touched on the influence of different constructions of woven glass on the breakdown strength of glass-reinforced laminates. It seems that a number of factors are involved, not only the weave but the fibre size, the geometrical arrangement and the glass pretreatment. Could the author let us have further information on such limitations? Finally has the author any experience of specimens igniting at failure?

Monsieur G. Lang (*France*): The method followed by my company seems to be related to that described in the paper.

We used the type of cell shown in Fig. F. The measurement circuit consists of an impulse counter and an oscilloscope. Recently the oscilloscope was replaced by a voltameter on which we are still carrying out improvements. The electrodes consist of a 3 mm-diameter spherical cap and a 3 mm-diameter cylindrical electrode. A neon tube indicates when the puncture occurs.

After preliminary tests, we came to the conclusion that the electric charge was a significant factor in the mechanical wear of the insulation. Wear has a chemical origin; it should be considered separately, and for this we use another type of cell.

We have verified our conclusions on Mylar, and work is in progress to check them on other materials. We have found that the electric charge required to break down a given thickness of insulation is independent of frequency (up to 800 cycles), applied voltage and thickness of the air gap. It is, however, necessary to realize some important points:

The applied voltage affects the number of discharges per supply-voltage alternation.

The air-gap affects the amplitude of these elementary discharges.

The voltage applied to the system is sufficiently low, 1–2 kV, for the breakdown to be due almost solely to the mechanical wear of the insulation caused by the electronic bombardment.

The thickness of the insulation considered is equal to the thickness of the sample less the residual thickness at the exact instant of the breakdown.

The amplitude of the discharges is a function of the state of wear of the insulation. It is therefore necessary to integrate the electric charges when making the measurement.

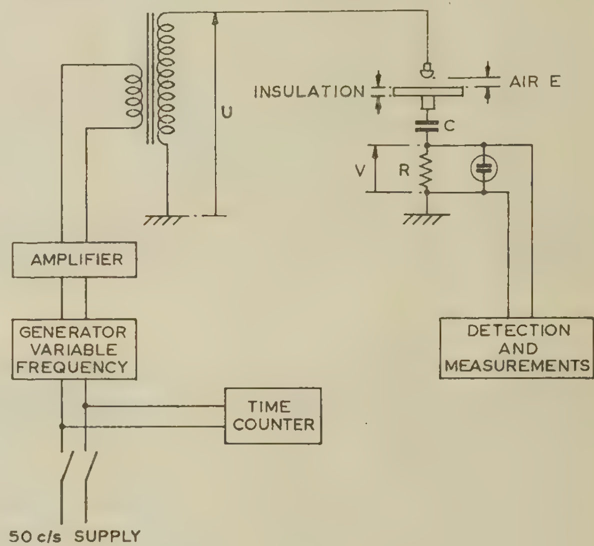


Fig. F.—Ionization cell.

Like the author, we encountered measurement difficulties. The first is the accurate measurement of electric charge together with the variation of the amplitude of the discharges with time. In my opinion, the latter may be ascribed to two main causes:

The enlargement of the air-gap due to insulation wear as stated.

The variation of the surface resistivity of the insulation.

These variations will probably have no effect on the measurement when we are able to integrate the charges accurately.

Mr. W. P. Baker: Do the tests recommended in the paper put the lives of insulating materials in the same order as they would be in service? Tests with needle electrodes as described in Reference 16 of the paper indicate that they do not. In particular, three materials in Fig. 2 can be shown to have relative lives at 80 volts/mil which are in the reverse order from those given in the paper.

Clearly, meaningful results are only obtained with tests which are carried out at about the working stress, and the maximum possible reliable acceleration of the test is necessary in order to minimize the delay in obtaining the information, for despite what the author states in the Conclusions we cannot wait 30 years to verify the test made in the laboratory.

We have come to the conclusion that gramophone-needle electrodes offer the best means of meeting these requirements. Under similar test conditions they yield results which are very close to those quoted in the paper, and their advantages may be summarized as follows:

They are cheap and precise.

As a result of the small area which is discharged at the point of the needle, thermal effects are small, and by operation at high frequency, 50 times the acceleration of life is possible.

The amount of chemical by-product is much reduced, and the problem of ventilation is much less severe.

Finally it must be pointed out that, although these remarks may seem very critical, had not the results of the author's work over the last decade been freely available, no one would have been in a position to offer any criticism.

Mr. N. Parkman: It is probable that the increased rate of breakdown for samples in mechanical tension is due to a mechanism similar to that put forward for cracking in mechanically stressed rubber exposed to certain gases. The dynamic physical picture sometimes advanced is one in which adjacent parts of molecular bonds broken by receipt of sufficient energy are pulled away from one another before chemical recombination can take place. Thus the rate of permanent bond cleavage increases, and with it the rate of degradation and breakdown. The mechanism is one of chain scission rather than of bulk movement of molecules as suggested by an earlier speaker.

Many people have pointed out the difference between service conditions and those obtaining in the tests described in the paper. This is a criticism which those who do this sort of work well realize, and it is important to remember that it can be levelled at any accelerated test. For years people have carried out accelerated tests to assess the resistance of materials to sunlight by exposing them to the rays emanating from an electric arc. This puts sunlight into the sample at a greater rate than it will meet in service in this country or probably anywhere. Nevertheless the results are very informative and have given guidance to designers. Similarly, heat-ageing tests are sometimes accelerated by raising the temperature above that found in service, but here again very valuable results emerge from this form of life test. In the accelerated discharge-resistance tests described in the paper, the aim has been to carry out the test in a way which will give at least a measure of control over the important degrading parameters. This makes for ease of interpretation of the results but inevitably gives rise to the type of criticism already referred to.

I think the paper should have emphasized rather more the danger of concluding that if, as a result of an accelerated discharge-resistance test at high temperature, material A appears to be rather better than material B, then, in service, material A will, in fact, behave better than material B. This tacitly implies that the heat-ageing characteristics of the materials are similar or that heat ageing is not dependent on time beyond the times involved in the discharge test. Both assumptions will generally be untrue. Therefore, if one carries out this test for the purpose of assessing discharge resistance at high temperature it is essential to run a parallel series of tests to assess the resistance of the material to heat ageing. In fact, it may be preferable to carry out discharge-resistance tests before and after a period of heat ageing, as suggested in E.R.A. Report Ref. L/T 382.

The choice of stainless steel as an electrode material has been made after many years of work using a great many other materials, and it is useful as a primary standard. In other cases, the use of different electrode materials will have a pronounced effect on the result. For example, it is known that brass and copper in contact with polypropylene at high temperatures have a marked effect on its degradation. Therefore, discharge-resistance tests carried out using other electrode materials may produce optimistic results especially if the tests are of long duration. Other examples exist, and the reasons for using the relevant electrode material, as suggested in the paper, are therefore predominantly chemical ones.

Mr. J. K. Wood: A certain amount of work has been done on solid glass. I wonder whether there are any results from this particular method of test which would indicate the relative resistance of solid glass to the discharges in order to compare them with other materials which have been tested. I should like to have some results of the same test applied to rather old-fashioned materials, and not the modern polymers, so that we could compare the results on old-fashioned materials against their known performance in service.

Discharge erosion patterns on solid glass have been observed, and I suggest that, with the same method of test, they would resemble quite closely those described by previous speakers on polymer materials.

THE AUTHOR'S REPLY TO THE ABOVE DISCUSSION

Dr. J. H. Mason (*in reply*): The need for tests which simulate service conditions has been raised by several speakers, but, as Mr. Baker said, we cannot wait 30 years for results. Thus the rate of deterioration during test must be accelerated, but then the results may give a false indication of the relative behaviour of materials in service, unless all factors which affect the various processes of deterioration and failure are taken into account. Analytical studies under somewhat idealized conditions are thus essential to the development of appropriate tests. Since some factors, e.g. increase in temperature, may have quite a different effect on the rate of deterioration of materials by discharges, thermal degradation and chemical attack, it is necessary to assess the resistance of materials to each form of deterioration separately, before their combined effects are investigated. I am particularly grateful to Monsieur Leroy and Mme Fallou for their account of tests to assess chemical deterioration by discharges, and I am sure that many engineers will welcome a more detailed account of their results. Wider publication should also be given to Mr. Parkman's reports on the effects of temperature and thermal ageing on the dielectric properties and the resistance to discharges and to tracking of various moulded insulating materials (namely, E.R.A. Reports Ref. L/T 361, 364 and 389).

Mr. Stark questions whether it is really necessary to pass dry air over specimens during discharge resistance tests. The answer depends on the thickness and nature of the test specimen, on the ratio of the applied voltage to the discharge inception value, V_i, and also on the natural variations in ambient humidity which may otherwise occur. McMahon *et al.*[10] found that high humidity greatly increased the life of 10 mil polythene specimens. E.R.A. investigations on polymer films showed that some specimens tested in the open laboratory failed in about the same time as others tested in dry air, but the average life and the spread in results were much greater when the humidity was high. Visual observation shows that after some hours discharges in static air occur mainly around a semiconducting surface layer which gradually increases in radius, so that new areas are always being exposed to discharges. Such effects are particularly serious with frequency-accelerated tests and at voltages below $2V_i$, whereas Fig. A shows that Mr. Stark's tests were made at 50 c/s and $5V_i$.

Throughout the paper it is emphasized that comparison should be made only between materials of similar thickness, and proof tests should be made on samples of approximately the thickness required in service. The different forms of erosion shown in Fig. B may arise partly from variations in discharge inception stress with thickness, so that at a given stress the discharge repetition frequency will increase with specimen thickness. It can also be shown that stress concentration at the end of discharge channels increases with specimen thickness (cf. Reference 1), so that, in thicker specimens, failure by channel propagation may occur without much previous erosion as in Fig. B(iii).

I agree with Mr. Baker that samples should be tested at about the stresses used in service, and it is a good idea to use a higher

frequency for tests at lower stresses (<40 kV/cm) as there is less chance of breakdown by cumulative heating than at higher stress. Comparison of results with point electrodes (cf. Reference 16) with those in the paper, shows remarkably similar assessment of the relative discharge resistance of various materials, so that either system could be standardized for thicker rigid materials. With thin plastic materials, however, there might be some difficulty in mounting point electrodes so as to avoid mechanical damage to specimens.

With regard to Mr. Simons's question on the effect of fibre size and glass pretreatment on the electric strength of glass-reinforced laminates, and also Mr. Wood's question on solid glass, I regret that I have no additional information available on these materials.

DISCUSSION ON THE STARTING OF INDUCTION MOTORS*

Dr. J. Štěpina (*Czechoslovakia: communicated*): In the first paper there is expressed the relationship between the starting performance of a 3-phase induction motor, when connected to a single-phase supply system, and its normal starting performance in terms of two dimensionless parameters. The second paper is an extension of the method to cover the starting of asymmetrical 2-phase induction machines from a single-phase supply, and it is shown that, in this case, the equations contain three dimensionless parameters. I would like to point out that in my publications[A–D] I have used nearly the same parameters as the authors. The main purpose of my discussion is to show that a modification of the developed theory makes it possible to determine all main unknown quantities by means of a single graphical construction which is applicable to machines of any rating in the same way as the theory of the authors.

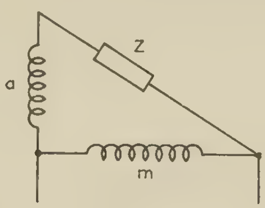


Fig. A

Inspection equations for the asymmetrical 2-phase induction motor in Fig. A are as follows:

$$I_1 = \frac{V}{2}\left(\frac{1}{Z_s} - j\frac{1}{k}\frac{1}{Z' + Z_s}\right)$$

$$I_2 = \frac{V}{2}\left(\frac{1}{Z_s} + j\frac{1}{k}\frac{1}{Z' + Z_s}\right)$$

where the same symbols are used as in the second paper, except that Z_s and Z' are used instead of Y_s and Y.

$$Z_s = \frac{1}{Y_s} \qquad Z' = \frac{Z}{k^2} = \frac{1}{k^2 Y}$$

By utilizing the substitutions

$$\frac{|Z'|}{|Z_s|} = \frac{|Y_s|}{k^2|Y|} = y' = \frac{1}{k^2}y$$

$$\frac{V}{Z_s} = I_m$$

we get

$$I_1 = \frac{1}{2}I_m\left[1 + \frac{1}{k}\frac{1}{y'\varepsilon^{j(\pi/2-\alpha)} + \varepsilon^{j\pi/2}}\right]$$

$$I_2 = \frac{1}{2}I_m\left[1 - \frac{1}{k}\frac{1}{y'\varepsilon^{j(\pi/2-\alpha)} + \varepsilon^{j\pi/2}}\right]$$

The graphical construction in Fig. B can be based on these equations. We put there

$$\mathrm{AO} = \frac{1}{2}I_m \quad \mathrm{OB} = \frac{1}{2}I_m \quad \frac{\mathrm{CG}}{\mathrm{OC}} = y' \quad \mathrm{OD} = \frac{I_m}{2k}$$

The symmetrical component currents are

$$I_1 = \mathrm{AE}$$

$$I_2 = \mathrm{AF} = \mathrm{EB}$$

The ratio of the starting torque, T, to the starting torque under balanced 2-phase conditions, T_b, is given by the expression

$$\frac{T}{T_b} = \frac{|I_1|^2 - |I_2|^2}{|I_m|^2} = \mathscr{R}\left[\frac{1}{k}\frac{1}{y'\varepsilon^{j(\pi/2-\alpha)} + \varepsilon^{j\pi/2}}\right] = \frac{\mathrm{EH}}{\mathrm{OB}}$$

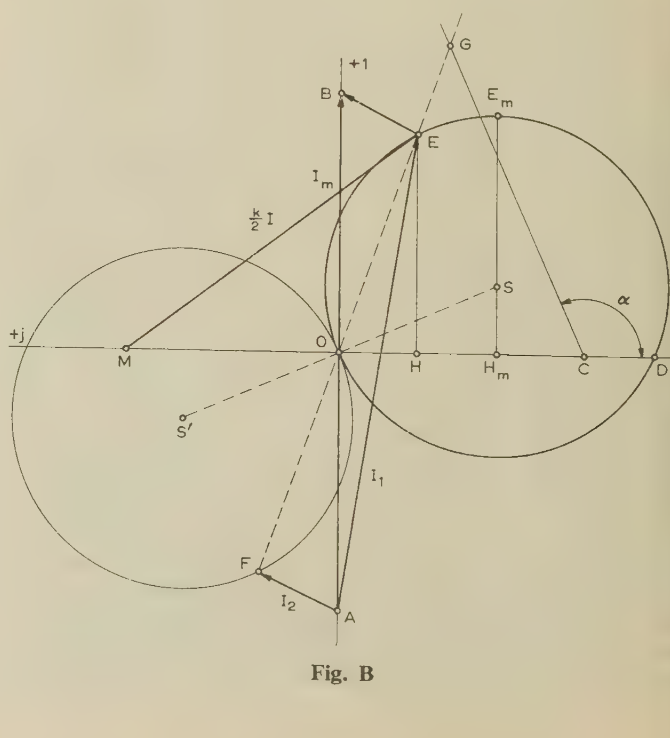


Fig. B

* Brown, J. E., and Jha, C. S.; Paper No. 2860 U, April, 1959 (see 106 A, p. 183). Jha, C. S., and Daniels, A. R.; Paper No. 3049 U, August, 1959 (see 106 A, p. 326).

The maximum torque ratio is then given by

$$\left(\frac{T}{T_b}\right)_{max} = \frac{\mathrm{E_mH_m}}{\mathrm{OB}}$$

For determining the current drawn from the supply at standstill we mark on the imaginary axis the length

$$\mathrm{OM} = \frac{k}{2}|I_m|$$

The supply current is then

$$|I| = \frac{2}{k}\mathrm{ME}$$

and the current ratio

$$\frac{|I|}{|I_m|} = \frac{1}{k}\frac{\mathrm{ME}}{\mathrm{OB}}$$

The derived diagrams may be used in various forms as is shown in References A–D. In Reference D, I have based on this diagram the construction of a nomograph for designing single-phase motors. In Reference C, I have derived Tables of the parameters k and y' (for given α and T/T_b), for which the starting quality is best.

Another advantage of dimensionless parameters is the possibility of including in the calculation the differences of weights of the main and auxiliary winding conductors and the loss angle of the capacitor. A quite similar diagram applies to a 3-phase induction motor connected to a single-phase supply system.

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(C) ŠTĚPINA, J.: 'Design of the Auxiliary Winding of Single-phase Induction Motors', *Elektrotechnický obzor*, 1958, **47**, p. 91.

(D) ŠTĚPINA, J.: 'The Diagrammatic Determination of the Starting Torque and Current of Single-Phase Induction Motors with Auxiliary Windings', *ibid.*, 1958, **47**, p. T78.

Dr. J. E. Brown, Mr. C. S. Jha and **Mr. A. R. Daniels** (*in reply*): We are grateful to Dr. Štěpina for drawing our attention to the work he has done on the problem of starting single-phase induction motors. The general equations derived in our papers are very amenable to graphical analysis, and the treatment developed by Dr. Štěpina in his series of papers is particularly interesting. One of the authors (A. R. D.) and his colleagues have investigated the possibilities of graphical methods, and have shown that it is possible to display the performance associated with the variations of the parameters y and α, for constant values of k, by means of sets of orthogonal circles.* A full account will shortly be offered for publication.

It should perhaps be pointed out that the value of graphical methods to a designer lies in the fact that a great deal of essential information can be presented in a single diagram. However, the final performance results, computed by this or any of the many alternative methods, can be tabulated or presented on graphs once and for all. Given these results the single-phase starting performance of any machine can be read off directly, provided only that the performance of the comparable machine under balanced conditions is known. These results are given in our papers, and by Dr. Štěpina in his References C and D. The Table of optimum values in Reference C, being particularly comprehensive, is extremely useful.

* RYE, J.: 'A New Method of Analysis for Single-Phase Motors', paper read before the Bristol Graduate and Student Section of The Institution 13th January, 1960.

DISCUSSION ON 'THE INFLUENCE OF CONSUMERS' LOAD/CONSUMPTION CHARACTERISTICS ON METERING PRACTICE'*

Before the NORTH-WESTERN MEASUREMENT AND CONTROL GROUP *at* MANCHESTER *27th October*, 1959, *and the* SHEFFIELD SUB-CENTRE, *at* SHEFFIELD *20th January*, 1960.

Mr. H. A. Davies (*at Manchester*): Calculations in the Appendix would appear to give a wrong impression.

In example 2, which estimates the recertification period assuming that all the registration was below 10% rated load current, a weighted error is used derived from the 5% load error. It would appear more reasonable to use a typical consumption for a very small consumer, say 100 kWh per year, rather than the national average of 1 540 kWh per year. If 100 kWh per year were used, the meter life would be about four times as long as that stated.

The method used to estimate the recertification period in example 1 does not appear to be valid. The percentage of meters outside the certification limits increases with each year of service, as does the average error and the standard deviation. The method will give a different meter life for different years of service. For example, if we assume that the change of error with years of service is linear over, say, the first ten years, then, in the example given, after ten years the average error of total registration is approximately $-2 \cdot 0$, and the standard deviation of total registration is approximately $1 \cdot 3$. The percentage of meters outside the $-3 \cdot 5\%$ limit is now approximately 12% and the average error of these meters is approximately $4 \cdot 0\%$. Substituting $y = -4 \cdot 0$ and $a = 12$ in the equation, we now get a calculated life of 25 years. Obviously in the early stages of the meter life when no meters are outside the $-3 \cdot 5\%$ limit, T is infinite.

Mr. E. Roscoe (*at Manchester*): The author deserves credit for drawing attention to certain facts although the paper would appear to cover too many aspects. Conclusions are difficult to draw from the results of so few tests.

It is important to ascertain the performance of each make and type separately, since to group them is misleading. Though the Ministry of Power may fix a common certification period for all makes and types, one must have regard to the accuracy of individual consumers' meters.

The supply undertaking's revenue depends on average performance, and would not be affected if one half of the meters were registering low, provided that the other half registered high by the same amount. It is known that some makes and types of meter are not as stable as others and that some register low after a time while others register high.

* GOLDS, L. B. S.: Paper No. 2863 M, February, 1959 (see 106 A, p. 342).

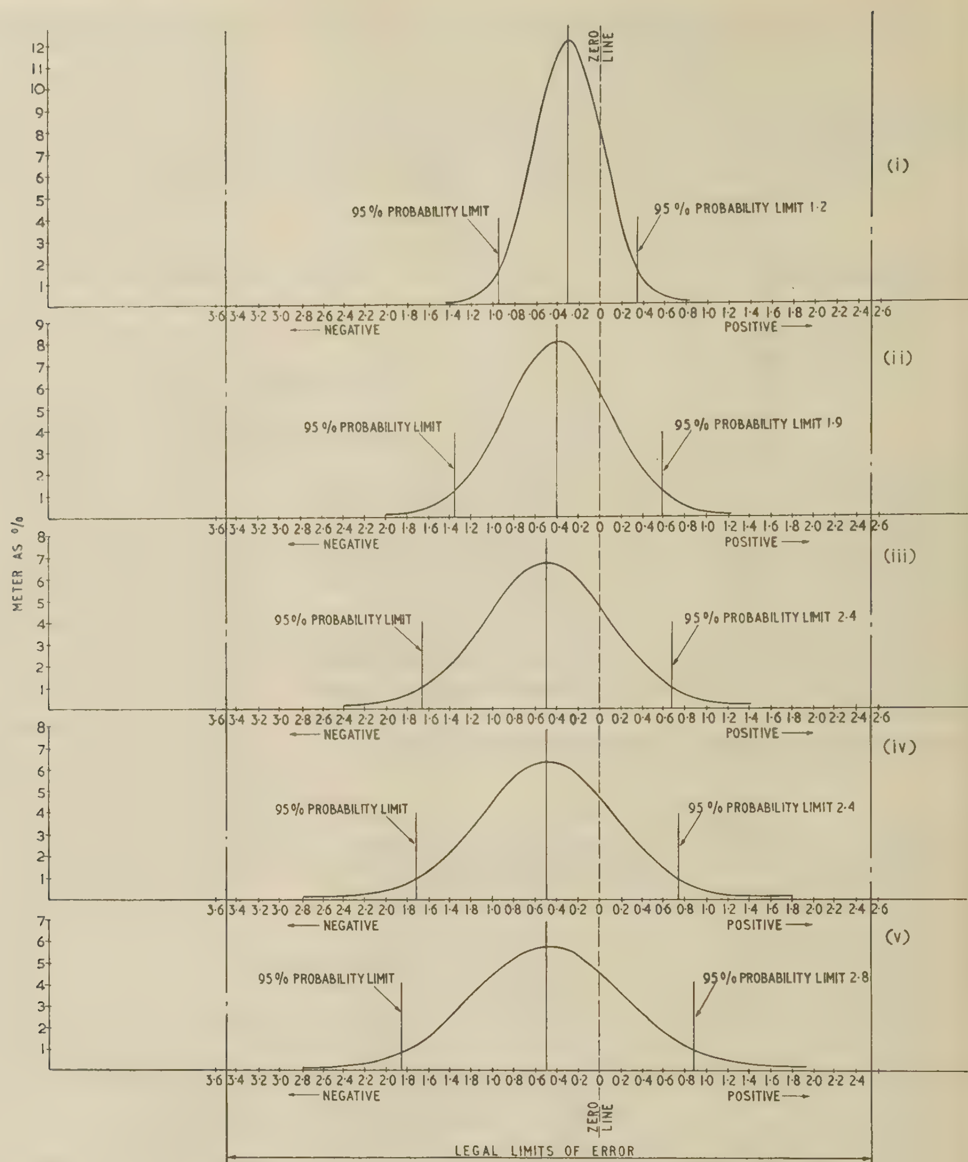

Fig. A.—Weighted percentage errors derived from 0·2 (100% L%E) + 0·6 (25%) +0·2 (5% L%).

(i) New from makers.
(ii) After one year on circuit.
(iii) After two years on circuit.
(iv) After three years on circuit.
(v) After four years on circuit.

Fig. A shows a method of graphically indicating performance of two makes of meters over six years' service. The point of 95% probability is marked. The stability, and therefore the quality, of each type is indicated by the consistency of the shape of these curves.

It is suggested that the performance might best be determined by a laboratory test on representative samples compared with a small number of meters removed from circuit. This method would enable the errors due to transport of meters and normal instability to be separated.

The author recommends a meter rated at 20 amp. However, a meter after being once repaired may have 30 years' service life. Social changes over this period due to legislation such as the Clean Air Bill will require current ratings of cables as well as meters to be higher.

Mr. H. Beattie (*at Sheffield*): I find the fourth paragraph of Section 2.5 rather confusing. It appears that removal errors were determined by load-analysing meters of unknown accuracy. The histograms show a pattern which most meter engineers would expect having due regard to a further unknown accuracy, i.e. initial calibration.

In Section 2.7 the author refers to 'maximum sustained current' peaks lasting for 'fractions of an hour'. These are rather loose phrases, and we would be on safer ground measuring demand over 30 min periods using Merz pattern indicators.

The first paragraph of Section 3.2 spotlights a weakness in the legislation covering meters in dispute. It is possible for a check test to be carried out without the meter operating within its accuracy range. It does not need statistics to prove that low consumption invariably follows the insertion of a check meter. Could the author suggest something more precise than the usual practice of giving testing engineers the duty of deciding whether load conditions are acceptable? Incidentally, I have never understood the reason for the $\frac{1}{2}$% bias in the tolerance band, and I would welcome limits of at least, say, ± 3%.

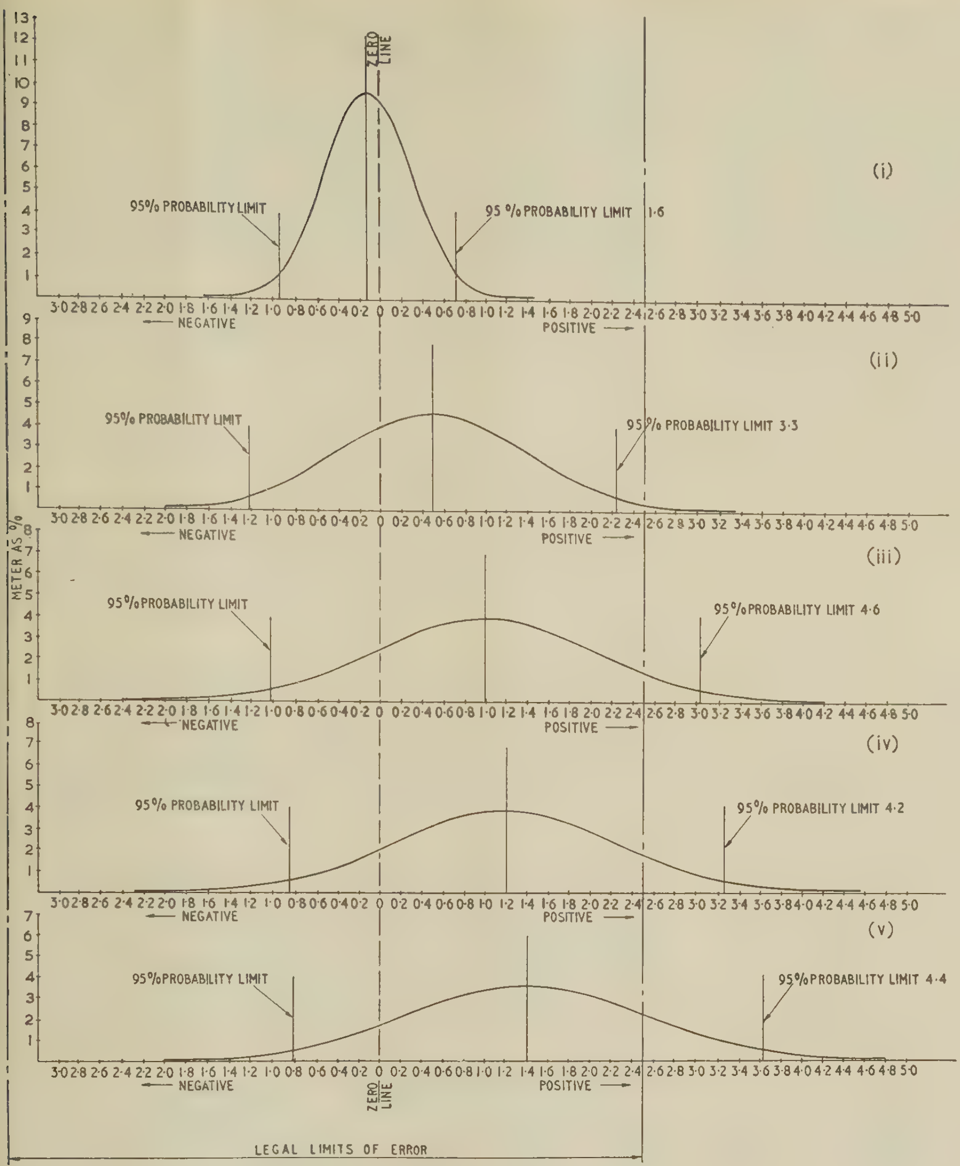

Fig. A—*continued*

It would have been better if the whole of Section 3.7.3 had been omitted and the paper confined to domestic meters. Polyphase metering presents quite different problems.

Reference is made to kWh meters with kVA indicators. Presumably Table 5 refers to such a meter and assumes identical load factors for each group. If so, the instrument-transformer ratios would be directly related to the consumption, giving approximate constant meter loadings. Thus the frequency of periodic testing given in the Table is governed by revenue. On the other hand, if the Table refers only to one size of meter for all groups, the consumption range of 100 : 1 is very large and would warrant revised metering. The author is no doubt aware of the difficulties associated with replacing (and possibly certifying) transformer-operated metering.

With regard to Section 4, I consider that the case is well proved for deciding on a 50 amp (m.c.r.) meter for future ratings, and a 100 amp (m.c.r.) meter for small commercial premises, to avoid the use of current transformers.

Mr. G. F. L. Dixon (*at Sheffield*): A certain element of arbitrary choice has crept into Section 3.6. In effect, the cost-balance principle underlying eqn. (2) is extended in an attempt to fix rational recertification periods. The author accordingly suggests that the sterling value of all energy being registered outside statutory limits should be balanced against the cost of recertification. I think from the context that he means the arithmetic sum of all energy registered outside limits, but, if the logic which led up to eqn. (2) is consistently extended, one would have to agree that the sum in question should be the algebraic sum.

In any case an Electricity Board cannot, in the long term, lose money. When we make meters more accurate we are not saving revenue losses so much as spreading electricity charges more equitably. I think that the whole problem will eventually have to be viewed against a wider economic background.

I regard Fig. 10 as constituting good support for the choice of the 40 amp m.c.r. meter as the national domestic standard, if one takes into account the normal load growth during the life of a meter. With regard to the suggestion of using old long-range meters with low nominal current ratings, if one takes into account the consequent store-keeping and administrative

problems and the costs entailed by additional meter changes, the expected gains may well vanish.

Mr. L. B. S. Golds (*in reply*): In reply to Mr. Davies, the same figure of annual consumption in example 2 as in example 1 in the Appendix was used in order to indicate the marked effect on the economic servicing period due to a change in one factor only in the equation, i.e. the average error of meters outside limits. In all four samples there was some 'over metering' and there were cases where annual consumptions exceeding 1 000 kWh per annum were registered mainly below 25% of the rated current of the meter (LR). A more reasonable consumption would be 500 kWh per annum rather than 100 kWh per annum, as proposed by Mr. Davies. Conversely, if the consumption in example 1 were 10 000 kWh per annum the period would be reduced from 73 to 29 years approximately. The nomogram of Fig. B illustrates the relation between loss of revenue, cost of maintenance and economic service period.

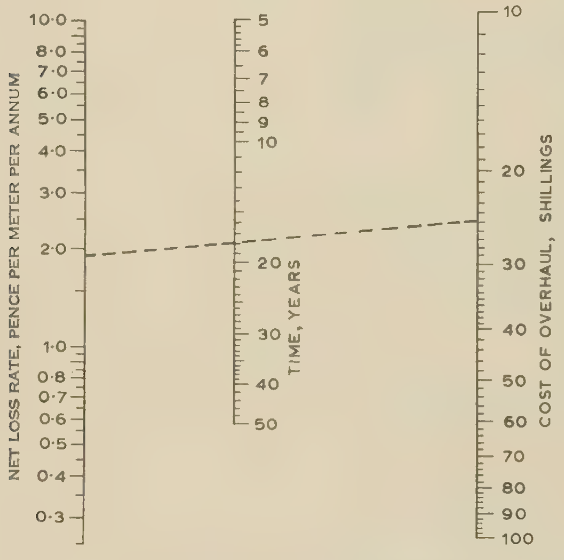

Fig. B.—Meter service period nomogram.

I agree that my proposal for certification-period assessment is indeed open to argument, but some rough justice must be tolerated. Nevertheless the period selected should be based on economics so that the costs of metering millions of domestic supplies are reasonable.

As all the aspects touched on in the paper are affected to varying degrees by the results of the load-consumption analysis I cannot agree with Mr. Roscoe that the paper covers too many. On the contrary I consider I would have failed had I not drawn attention to the complex nature of the problem of metering electricity supplies efficiently and equitably.

Conclusions can be drawn from the results of the tests despite the small number in the samples, since the 90% probability figures of the average consumptions in the various sub-ranges are within 5% of the average figures shown. Where in an area the metering data, saturation factors of appliances and average consumption are similar to those given in Table 1, the relevant load/consumption characteristics are then given in Table 2.

In fixing a certification period it is important to ascertain the performance of each make and type of meter separately, but it is essential to know the load/consumption characteristics in each class of consumer in order to assess the total registration error of each make and type.

Each of the four samples shows on average that the revenue derived from meters registering high does not compensate for those registering low and figures are given in the last two paragraphs, respectively, of Section 2.5. It is also clear from the data given in this and the paper describing the pilot test that some types of meter are not as stable in performance over long periods as others. The frequency-distribution method of presentation of meter errors for demonstrating these differences is most effective, but the standard deviation states it quantitatively.

Many factors influence the errors of meters, including transport, and the average effect of this on meters at three loads is indicated in Table 4.

In common with many others Mr. Roscoe appears to have overlooked the significance of Fig. 6. There are, of course, other forms of heating and cooking which can meet the requirements of the Clean Air Bill referred to. Perhaps these other forms are not as convenient or efficient as electricity, but nevertheless they can affect its use. Furthermore, diversity is a major factor affecting metering practice.

In reply to Mr. Beattie, Table 4 shows the average errors on site obtained by tests against a substandard at the site voltage, and the tests on the bench against the same substandard and at the same voltage. Most of the tests were made on certified meters and hence the initial calibration errors were known.

Load-analysing meters with self-contained maximum-demand indicators would be an obvious improvement, but capital was in short supply when I initiated this investigation, and it happened that the thermal-demand indicators were already available. The demands indicated would, I believe, show lower values if maximum-demand indicators with 30 min integration periods were employed rather than 20 min thermal-demand indicators.

I have suggested in Fig. 9 a guide to meter ratings. The investigation using maximum-demand indicators can, of course, be repeated on a random sample of consumers in any area, and a table of meter ratings for certain ranges of annual consumption can be prepared. The $\frac{1}{2}$% bias was proposed by the former Electricity Commissioners to take account of the known tendency of meters to under-register. I agree that ± 3% would be more equitable as statutory tolerances.

Table 5 refers to one type of transformer-operated meter and assumes identical load factors for each group. In fact, the interval between check tests is governed *inter alia* by revenue derived from consumption. Such metering as is referred to in this Section does not fall within the sphere of periodic meter certification, which was designed originally for the ordinary consumer, however the instruments must be of an approved type (Section 59 of the 1899 Clauses Act).

Meter rating is closely associated with meter design in terms of friction and torque as well as the load to be metered. I cannot agree with the suggestion to use a 100 amp meter to avoid the use of current transformers, which are generally more conveniently installed than are 100 amp meters.

Mr. Dixon suggests that, in attempting to fix a rational recertification period, one should use the algebraic sum of all the energy registered outside statutory limits as the basis for arriving at the value to be inserted in eqn. (2), whereas in the paper I suggest that it should be the arithmetic sum. The use of the algebraic sum is similar to accepting the principle that larcency does not matter since the net gain to the population as a whole is zero. On the other hand, I suggest that the arithmetic sum provides a more equitable method of dealing with this problem as between individual consumers.

I agree that an Electricity Board does not lose money in the long term. We must remember, however, that an Electricity Board purports to sell energy at a certain price per kWh. If the

meters are on average under-registering the true consumption, the value registered is, in fact, variable and not related to a statutory kilowatt-hour. Therefore, raising the price to compensate for such under-registration could not be condoned. Quite apart from the meters registering bogus values of the legal kilowatt-hour, the raising of prices should be the last resort, since this has the overall and long-term effect of restricting development of electricity usage. The neglect of meter maintenance is wrong from the consumer's point of view, it misleads the Electricity Board as to the amount of energy sold, and does not allow the efficiency to be measured accurately. I conclude that there is a loss of revenue where meters show a net under-registration, since, under the tariff, the Board is not collecting the revenue it should do.

I do not agree that Fig. 10 supports the choice of 40 amp m.c.r. as the only national domestic standard meter rating, unless the full-load torque is raised, or some artifice is used to reduce the effect of friction on the existing relatively low-torque meters. Mr. Dixon might bear in mind that, although the industry has undergone great and rapid development, the bulk of the energy sold to-day on domestic tariffs is at currents under 20 amp, as shown in Fig. 8. Furthermore, although the annual consumption has increased, Fig. 10 shows that it has not been matched by a proportionate increase in demand.

DISCUSSION ON 'THE APPLICATION OF LINEAR INDUCTION MOTORS TO CONVEYORS'*

Dr. F. T. Barwell (*communicated*): I have considered the possibility of converting Dr. Laithwaite's linear motor for propulsion on high-speed railways. The first idea was to use a track with metal cross-sleepers as the 'rotor'. As the authors point out on page 285, the magnetic circuit of such a single-sided linear system would be extremely poor. However, if a plate-like 'rotor' could be erected in a vertical plane along the track and the vehicles provided with 'stators' on each side, something electrically similar to the authors' plate conveyor would result. This would relieve the motive power designer from dependence on adhesion and would enable regenerative braking to be obtained without additional apparatus.

Any economy in capital cost would be dependent on the elimination of gears and transmission, but unless the speed of the train corresponded to the peripheral speed of standard motors, the magnetic circuit would be less well employed with consequent loss in specific performance.

The proposal, therefore, relates to a hypothetical high-speed railway rather than to an existing system. Nevertheless, the problem of points and crossings has to be faced. A possible solution would be to subdivide the functions of carrying and guidance by eliminating flanges on the wheels. The carrying wheels would then only require a fixed track, in the form of a horizontal plane which would enable crossings to be safely accommodated. The central rotor would fulfil the guiding function, but at bifurcation of route, the plate on the non-operative route would have to be interrupted for a sufficient distance to permit the passage of the 'stator' as well as the carrying wheels. This presents a difficult design problem which might best be solved by providing two sections of guiding plate at the junction. The one not required would be lowered vertically into a suitably shaped recess in the roadbed so that its upper surface became flush with the running plane.

Speed control would be achieved by varying the frequency of supply to the 'stator' or by varying the effective number of poles.

Dr. E. R. Laithwaite and **Messrs. D. Tipping** and **D. E. Hesmondhalgh** (*in reply*): Dr. Barwell's suggestion is interesting and exciting. The use of linear motors for trains was first proposed just over 50 years ago, but not in the form suggested here. As Dr. Barwell points out, low-speed trains would not utilize the linear induction motor to best advantage, but speeds of the order of 150 m.p.h. would enable motors with a pole-pitch of 2 ft 6 in to be used. The fact that the track width would be limited to perhaps 6 in has been shown in the paper to be no real drawback, since this application would require a rotor of comparatively high resistance. Furthermore, with such a large pole-pitch, the air-gap of the machine could be made quite large without detracting a great deal from the performance. This would allow mechanical clearance for cornering. As regards points and crossings, the scheme might involve a linear motor on each coach so that a train could still be driven at half power, even if a section of centre rail equal to half the length of the train were omitted, although the idea of carrying wheels, perhaps with rubber tyres, running on a flat surface, with the driving rail used as guide, has more appeal, and the solution of the points question could well be solved by the movable sections, as Dr. Barwell suggests.

Speed control could be effected by winding the linear motor for phase-shift control as described in a recent paper.* This would give continuous control over the high speed range, and a pole-change might be employed for very low speed.

One of the most attractive features is that the rotor copper loss is left behind on the track and the train itself carries only the weight of the stator and is responsible for disposal of only the stator losses. Being independent of adhesion, the train could be of light-weight construction.

We feel that Dr. Barwell's suggestions should be pursued, and we propose to undertake further experiments. Clearly the central driving rail or plate could contain steel as well as copper or aluminium, but one of the questions which we hope our experimental results will answer is whether it could be composed entirely of steel.

The biggest problem we foresee at present in developing this scheme is that of obtaining a 3-phase supply on the train.

* Laithwaite, E. R., Tipping, D., and Hesmondhalgh, D. E.: Paper No. 3225 U, June, 1960 (see 107 A, p. 284).

* Williams, F. C., Laithwaite, E. R., Eastham, J. F., and Farrer, W.: 'Brushless Variable-Speed Induction Motors using Phase-Shift Control', *Proceedings I.E.E.*, Paper No. 3262 U, May, 1960 (108 A).

DISCUSSION ON

'RESEARCH ON THE PERFORMANCE OF HIGH-VOLTAGE INSULATORS IN POLLUTED ATMOSPHERES'*

Before the MERSEY AND NORTH WALES CENTRE *at* CHESTER *23rd November, the* SHEFFIELD SUB-CENTRE *at* SHEFFIELD *16th December,* 1959, *the* NORTH-EASTERN CENTRE *at* NEWCASTLE UPON TYNE *11th January, and the* NORTH-WESTERN SUPPLY GROUP *at* MANCHESTER *26th January,* 1960.

Mr. J. D. Qualtrough (*at Chester*): The most common form of pollution of overhead-line insulators in this part of the country is salt spray. For instance on one night in 1954 over 50 flashovers occurred on the 132 kV Grid system in the coastal area of the north-west, most flashovers occurring on tension insulator strings. On another occasion one line tripped 11 times in one night.

Most of the flashovers due to salt spray occur within about five miles from the sea. The average of 35 flashovers per 100 miles of 132 kV line due to salt spray is rather misleading as the probability of flashover under such conditions falls off rapidly with the distance of the line from the sea. Can the authors give any estimate of the probability of flashover due to salt spray based on the distances of the line from the sea?

The sea-bed off the north-west coast is very shallow for several miles off shore, and similar conditions exist in the Wash and the Thames Estuary. Do the authors think that these shoal waters have any significant effect on the severity of salt-spray storms?

A considerable improvement in insulator performance on the lines in the north-west has been effected by increasing the length of tension insulator strings by about 25% to give surface leakage paths of the order of 120–130 in.

Insulator strings are greased on a small number of towers. The practical difficulty of greasing overhead-line insulators is, however, considerable and can only be contemplated for a very limited number of cases.

I have experience at two Grid substations of inverted oil-filled insulators. In one station inverted insulators have given fairly satisfactory service for ten years, but much more maintenance has been required than was at first envisaged. The second installation was a failure owing to the effects of coke dust, and was reconverted to standard insulators after two years.

Twenty-four 33 kV sealing ends incorporating oil baths have been in service for five years with good results at a site where standard 33 kV sealing ends gave unreliable service. Transformer oil was blown out of the oil baths very readily but a more viscous oil has proved satisfactory. Part of the improvement in performance is undoubtedly due to the increased surface leakage paths of the oil-bath sealing end.

If oil-bath suspension insulator strings are used on overhead lines in polluted districts, what type of insulation would the authors propose for tension insulator strings? Do the authors in any case consider that the oil-bath principle has sufficient advantages to justify its continued use?

Grease has been used successfully on insulators in the north-west for some four years. It became apparent, however, that greases which were easier to apply, easier to remove, more resistant to rain washing and with adequate electrical characteristics were required. Consequently several greases were developed and tested on a very polluted site on single switchgear post insulators energized at 38 kV, which is double the service voltage, with a test arrangement similar to that used by the authors. Some of these greases are now being used in service.

I notice that the authors use the normal service voltages, and I would like to have their views on the use of double service voltages.

Whereas hand insulator cleaning is a palliative against industrial pollution, no protection is given against salt spray. I have known insulators to flash over on several occasions due to salt spray two or three days after hand cleaning. In any case, most insulators which flash over due to salt spray are in rural areas. I feel, therefore, that the authors' comments on hand insulator cleaning are somewhat misleading.

It is obvious that oil and grease treatments, although effective, are attended with considerable practical disadvantages, and I trust that satisfactory methods of voltage stabilization of insulation can be rapidly developed.

Mr. D. H. Braid (*at Sheffield*): It is unfortunate that the authors' research does not cover the voltages used for 'distribution', say from 66 kV downwards, since it is clear from Table 2 that, as voltages decrease, rod insulators become relatively more reliable. Lines at lower voltages are usually short, and cleaning is fairly easy to accomplish. Gangs have to be kept to deal with other maintenance and breakdowns. Thus routine hand cleaning is not as expensive as at first appears, and a simple insulator would obviously make the work easier.

The Sheffield area is extremely subject to acidic pollution, and the life of standard galvanizing is about three years. It is obvious, therefore, that frequent inspection of equipment is desirable, and owing to defects or damage being noticed when hand cleaning, it is probable that the general reliability of lines is improved thereby.

I was interested to hear of a possible revival in plastic insulators, since one of the major pollution troubles with which I had to deal concerned an early type of such insulators. Owing to a gale, there was fairly heavy pollution by salt, and when this was followed by slight fog, the insulators became charred, and serious damage to conductors resulted. The idea of plastic insulators is attractive, since they are unbreakable, and likely to be homogeneous, with foreseeable stress fields.

Another occasion was as the result of the dry summer of 1959, when some form of practically invisible pollution caused serious trouble with the arrival of the autumn fogs. Normally the heavy rains accompanying thunderstorms remove this pollution, and present cleaning programmes must be modified to prevent such trouble.

Dr. L. L. Alston (*at Newcastle upon Tyne*): With reference to Section 3.4, I agree that, at present, plastics are unsuitable for outdoor insulation. However, what are the authors' opinions on a recent French paper,* in which it is stated that plastic materials should soon be available for outdoor use?

It appears from Section 5.2 that flashover phenomena are similar on d.c. and on a.c. systems; nevertheless, it is stated in the Conclusions that 30% more insulation is required on d.c. systems. Can the authors elaborate on this?

* FORREST, J. S., LAMBETH, P. J., and OAKESHOTT, D. S.: Paper No. 3104 S, November, 1959 (see 107 A, p. 172).

* LAFONT, P.: 'Isolateurs en Résine Epoxyde. Comportement sous Pollution et Humidification', *Bulletin de la Société Française des Electriciens*, 1959, No. 105, p. 541.

Resistance glazes offer interesting possibilities, and it is unfortunate that the deterioration referred to by the authors makes them of doubtful use at present. It is remarkable that glazes should be effective in controlling the voltage gradient even when their resistivity is much greater than that of the deposit (Section 3.3). Was the resistivity of the deposit measured on energized resistance-glazed insulators? One wonders whether the heat produced by these glazes might not be as important as their voltage grading action, since heat would tend to keep the deposit dry, so that its resistivity would not, in fact, be low.

The current records obtained by the authors show that flashover occurs if the surges exceed a few hundred milliamperes. This observation was also made by other investigators, and the experimental technique of Reference 27 is based on it. The reasons for this critical current are not clear; they may be associated with glow-arc transitions, which are known to occur at these currents. These phenomena are being studied in our laboratory. In the first place, a simple experiment was made to assess the relative importance of the distortion of the voltage gradient and the current flowing in the pollution before flashover. The insulation consisted of a glass sheet, on which moist pollution was spread between U-shaped electrodes, as shown by the sketches of Fig. D, which are roughly to scale. The voltage

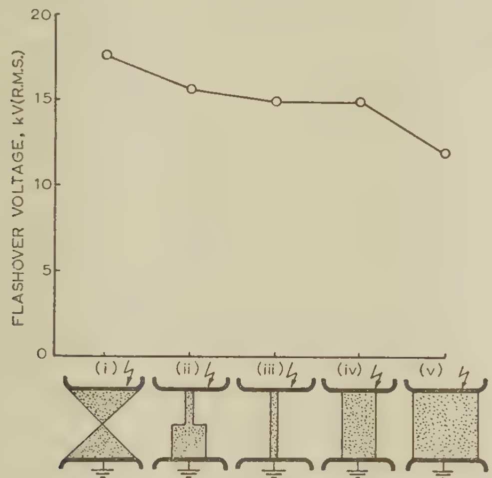

Fig. D.—Dependence of flashover voltage on the distribution of contamination.

distribution and the current before flashover depend on the shape and dimension of the polluted area, the resistance of the pollution per unit area being substantially constant. It is obvious that the voltage distribution is much less uniform for configuration (i) than for (v), but nevertheless (i) had a higher flashover voltage. This was ascribed to the fact that (i) had a higher resistance, and for the same voltage (i) took less current than (v) before flashover. Again, the voltage distribution was substantially uniform for (iii), (iv) and (v), but the resistance was progressively lower; it will be noted that the flashover was also lower. These results agree with similar data obtained by von Cron,* who concluded that flashover on site is more likely with insulators of larger diameter.

The mechanism of flashover can be examined by means of the high-speed camera, and Fig. E shows the records obtained between the instant of flashover and the preceding voltage zero, for discharges which had burned for several cycles. The electrodes have been indicated on the photographs, and the time interval between individual records was 0·9 millisec. It will be

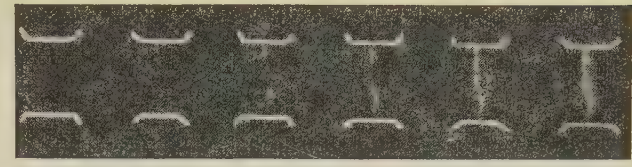

Fig. E.—High-speed camera record of the growth of discharges.

seen that two discharges develop near the electrodes, and grow in length and intensity as the current grows. Smaller discharges occur between the two main streamers, and ultimately the whole inter-electrode space is spanned by discharges and flashover occurs.

Mr. A. B. Wood (*at Newcastle upon Tyne*): My remarks are directed primarily towards transmission-line insulation. It is probably not generally appreciated just how much one has to pay for pollution in terms of increased cost of transmission lines.

Under clean atmospheric conditions the number of insulators required to co-ordinate with switching surges and power-frequency requirements at various line voltages is shown in Fig. F, together with a shaded area indicative of the additional

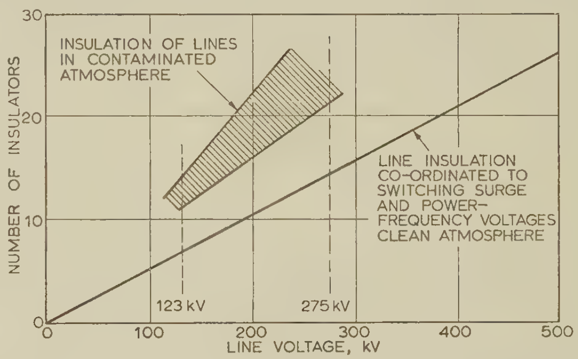

Fig. F.—Insulation of lines in contaminated atmosphere. Steel tower construction using suspension insulators. Standard units, 10 in × $5\frac{3}{4}$ in spacing.

insulation required under polluted conditions. This area should be regarded as only approximate since special long-leakage-path insulators will reduce the number of units required. Nevertheless at voltages of the order of 275 kV, about four extra discs would be likely to be necessary to cope with pollution.

The cost of this extra insulation is not significant, but the effect on the tower dimensions is, and very roughly speaking the tower weight for a typical double-circuit line might be about 8% greater owing to the increased length of the insulation. When it is realized that about half the total cost of such a transmission line is tower cost, the price we pay for operating under polluted conditions is considerable.

There is therefore some considerable point in research into methods of alleviating the effects of contamination on insulator strings. This is of such importance that I feel that the researches might have been carried out on a statistically larger sample; one cannot determine from the results the effects of shape and length of leakage path.

From the transmission-line point of view greasing or any other consumable surface treatment must only be regarded as the last straw—something to 'prop up' a line which has run into trouble. Important transmission lines will never be available to perform this expensive and time-consuming operation of surface treatment. I think that the authors are correct in describing greasing as a palliative.

For a.c. transmission I think that capacitance grading is a

* Von Cron, H.: 'Testing Insulators with Reproducible Foreign Layers on their Surface', C.I.G.R.É., Paris, 1956, Paper No. 203.

promising approach, and the fact that only a brief mention has been made in the paper is probably due to insufficient test evidence being available at the time. Can the authors add anything to this point?

Perhaps we might have more information on tension-set performance compared with suspension-set performance. The different orientation of the discs affects the natural washing, and tension sets usually perform better than suspension sets of the same units. Is this not a pointer to future designs?

Only very brief mention has been made of pin corrosion, and that in connection with d.c. systems. In the United States a special cap-and-pin insulator has been developed with a corrosion-inhibiting sleeve, which suggests that pin corrosion is perhaps more widespread than is generally realized. I do not think that this is a serious problem generally, but could the authors comment on the relative corrosion of the pins of cap-and-pin insulators in both clean and polluted atmospheres?

Mr. F. H. Birch (*at Newcastle upon Tyne*): Spates of insulator flashover have occurred recently at two outdoor 275 kV switching stations in north-east England. The flashovers were caused by a combination of industrial pollution and fog at one station and by a severe salt storm at the other. The two types of post insulator involved and the distribution of flashovers between them are shown in Table A, which also compares the leakage paths of these insulators with those of nearby overhead-line insulators which did not flashover.

Table A

Insulator type	Flashovers	Leakage path
	% of total	in
Post supporting circuit-breaker blast heads or series isolator contact	64	296
Post (upright or inverted) supporting busbar	28	275
Unknown (within zone of busbar protection)	8	—
Line suspension string	0	308
Line tension string	0	210

Can the authors state from their experience how long it would be necessary to make the leakage paths of the post insulators in order that their performance would be as good as that of the line insulators under the conditions experienced?

In Section 1 it is implied that it is necessary to increase the insulator leakage path rather more than in proportion to the applied voltage. Would the authors indicate, from their experience with insulators for 132 and 275 kV systems, how long a leakage path unstabilized insulators are likely to require to perform satisfactorily in polluted atmospheres on systems working at 380 and 500 kV?

Mr. R. G. Tee (*at Newcastle upon Tyne*): During site testing, do the authors monitor atmospheric conditions?

The Research and Development Department of the North Eastern and Yorkshire Region of the C.E.G.B. have set up a field testing site at a substation affected by industrial pollution. We will record the leakage current and surge amplitude of a number of identical insulators with different surface treatments and coatings. We will continuously record temperature, humidity, visibility, sulphur-dioxide concentration, wind direction and velocity, rainfall and barometric pressure. To differentiate between various fog conditions we intend to develop an instrument for continuously recording the conductivity of the aerosols and pollution particles in the atmosphere. The conductance of an unenergized insulator will also be recorded.

By these measurements we intend to correlate insulator performance with atmospheric and pollution conditions. It is hoped that this research will assist in the design of an apparatus for the rapid testing of high-voltage equipment under conditions simulating those which we find lead to insulation failure.

It would appear from the large number of insulator shapes already in existence, some of which are illustrated in the paper, that success is unlikely from this line of attack. The alteration of the surface properties of the insulator material by the application of grease has been shown by the authors to have beneficial effects. This could be due either to encapsulation of dirt particles or to the surface of the grease being hydrophobic. Mention has been made by a previous speaker of the possibility of using p.t.f.e. If grease is effective only because its hydrophobic properties prevent the formation of a continuous film of electrolyte on the surface, any shape which assists the natural shedding of water would be an advantage. Thus, if a permanent satisfactory surface treatment or coating which prevents the formation of a continuous film of electrolyte can be found, insulators need only be plain cylinders or truncated cones.

The surface treatments we will be trying at the test site are various chlorinated silanes, which form a chemically-bonded monomolecular hydrophobic layer and a p.t.f.e. coating.

Small-scale laboratory testing of surface coatings in a humidification cabinet will also be carried out. A laboratory examination is also to be undertaken on the possible oxidation of sulphur dioxide to sulphur trioxide in corona discharge.

Mr. F. S. Edwards (*at Manchester*): The paper describes in great detail the exhaustive researches which the authors have been carrying out for many years on an important subject and they have obtained some very useful information. In studying the results, however, I am constrained to wonder whether the counting of surges is adequate as a means of assessing insulator performance; very substantial variations are recorded in the behaviour of insulators which are virtually duplicates.

In a fairly long time—44 months in one case—there were 4 000 surges on one insulator and only 1 300 on another, without flashover on either insulator. This is a ratio of 3 : 1 in numbers of surges. I leave it to the statisticians to work out, if they can, the ratio to be expected if, for example, ten instead of two nominally identical insulators are tested and to say what is the minimum ratio of numbers of surges which would be significant as indicating a definite difference in performance. I am sure that the ratio would be considerably higher than 3 : 1. I have no doubt that the authors themselves have been disturbed by the random variations and anomalies which are mentioned in Section 1, and since they regard the number of surges counted as of considerable value I should welcome some amplification of the basis they adopt for assessing their significance.

A point not directly connected with the subject of the paper, but nevertheless closely allied to it, is the behaviour of dirty insulators on over-voltages from switching surges or lightning. It is evident that, when conditions are bad, the margin of power-frequency flashover voltage over the working voltage is very slight; does this mean that an over-voltage which would normally be suppressed by a surge diverter or limited by a rod-gap will inevitably cause flashover with the probability of destruction of the insulator? If so, are the authors content to have it so?

The authors are, of course, aware of the French work on the subject, as References 25–27 show. However, these and other References suggest that a slightly different approach has been made on the Continent. Gaillet[26] reports some results of the detailed examination of surge-current waveshapes with a cathode-ray oscillograph. Apart from a brief reference at the beginning and end of the paper, the present authors say nothing about the use of the cathode-ray oscillograph. Perhaps they considered it

less rewarding than other means of investigation. Josse* remarks upon the unsatisfactory results of his tests on polluted insulators—unsatisfactory in that they were very inconsistent—and he made some experiments with water of very low resistivity (200 ohm-cm) as a substitute for pollution; he claimed high consistency in his flashover voltages. Whether they were significant as an indication of service performance is another matter. Josse also mentioned a dew test in which the insulators were cooled to 0° C and then exposed to an atmosphere of high humidity.

These widely different ways of attacking the problem dealt with in the paper show by implication the difficulties which have been experienced in making satisfactory tests.

Mr. J. A. Spence (*at Manchester*): In the Introduction, the authors give, as a measure of the problem of insulation in polluted atmospheres, the number and rate of flashovers which have occurred. I would go further than that by including the amount of maintenance required to prevent flashovers. This can be very expensive, not so much in direct labour costs but in out-of-merit running costs. The order of these might well run into thousands of pounds sterling per day for 275 kV lines and equipment. Furthermore, it is when outages for maintenance occur that the security of supply is in great jeopardy. The greatest cause of interruption of supply is when a fault occurs with another circuit out for maintenance. Therefore, I believe that, in assessing the various types of insulators, the amount of maintenance required is a most important factor. I would therefore query the statement made in Section 3.5 of the paper and ask whether any economic appraisal has been made of increasing the insulation to an amount which would ensure that no flashovers occurred even in polluted atmospheres with yearly maintenance.

In Section 2 the authors give the test voltages used, which are the normal phase-to-earth working voltages for the Grid systems. Thus the tests at best can only prove a factor of safety of one. I doubt whether any such widely used equipment is tested to such a small factor of safety, and I suggest that twice working voltage should be used. Insulation thus tested can be trusted to give reliable performance at working voltages.

I do not agree with the authors that they are simulating the conditions experienced during a salt storm. I have found that the conditions which produce these storms are a high wind blowing from the sea unaccompanied by rain. The salt is deposited on the insulation and is immediately dried with the wind. Everything appears to be satisfactory until a drizzle falls or a dew occurs, which may be some days later. Under such conditions it is difficult to keep gear alive. Incidentally, tension strings are very prone to failure under these conditions, whereas they give the better performance under industrial pollution when suspension strings fail.

Greasing seems to offer a temporary solution to the problem, and I would have thought that this, combined with stabilized glaze, would have been even better. The authors show in Section 3.3 that this is not the case. I would like to have the reasons for this failure.

Mr. J. H. Pirie (*at Manchester*): In Section 3.1.1 in discussing the effect of insulator shape the authors state that the performance of a 6-unit glass insulator string is slightly superior to a corresponding porcelain insulator string. Bearing in mind that the distance between the centres of the former is 6 in as compared with $5\frac{1}{2}$ in for the latter and there is a noticeable difference in the design of the shedding, I should like to ask the authors whether they would not attribute the relative performances almost solely to the different geometries of the two strings with little or no bearing on their insulating material.

* *Bulletin de la Société Française des Electriciens*, 1958, 8, p. 517.

On the question of oil-bath post insulators in Section 3.2.1.1, as no Table of relative performances is given, the following figures for unit Nos. 1, 2, 6 and 7 in Fig. 4 taken over a period of two years may be of interest:

Unit No. 1	0 surge counts
Unit No. 2	12 surge counts
Unit No. 6	20 surge counts
Unit No. 7	252 surge counts

A rather significant feature which was only brought to light two months later when unit No. 2 was suspected of giving trouble was found on examination to be due to the fact that the oil-filled section was so full of water and carbonaceous deposit that it was no longer of any value as an insulator. This lends weight to the authors' opinion that the oil-bath is chiefly a reservoir to maintain an oil-covered water-repellant surface over a reasonable leakage path.

I should like to have the authors' opinion on the use of this type of insulator in the vicinity of cement works.

Dr. H. F. Maass (*at Manchester*): Specific creepage distances of 1 in/kV total and $\frac{1}{2}$ in/kV protected have been adopted in this country as a result of the authors' work. The height of apparatus insulators necessary to accommodate these values exceeds, at voltages of 132 kV, quite considerably the height required to meet accepted impulse levels. The Conclusions refer to a 'factor of merit' and do not restate explicitly these creepage requirements. I presume, however, that this is not intended to mean that shortened apparatus insulators, with the factor of merit of approximately 2·5 obtainable with established anti-fog shed designs, are going to be acceptable.

It is also evident that creepage distances are not the only parameters to be considered. Some insulators (such as No. 9, Fig. 3) meeting them are unsatisfactory; others may well be satisfactory with lower creepage distances.

It is desirable to collect as much factual information as possible. I can contribute information on four 132 kV insulators (an air-blast circuit-breaker support insulator, two torque insulators with plain and with anti-fog sheds and a plain shedded insulator used indoors) shown in Fig. G, which were tested by the authors at Croydon. Table B includes some additional

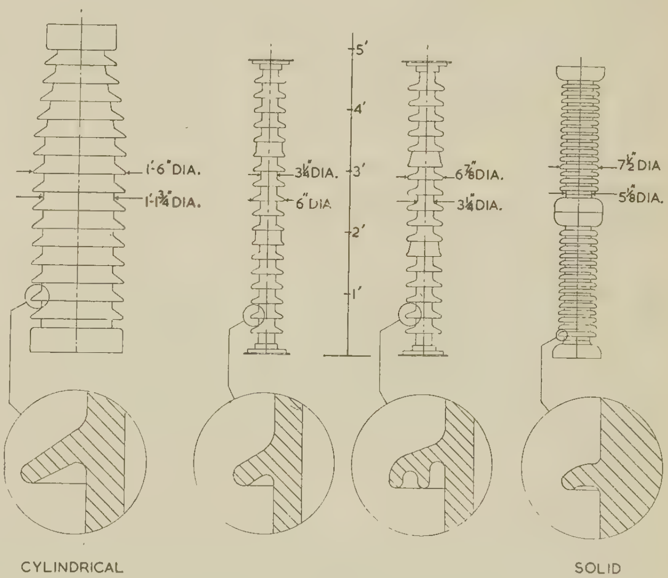

Fig. G.—132 kV insulators tested at Croydon.

Table B

132 kV INSULATORS TESTED AT CROYDON

Type of insulator	Length	Creepage distance		Total creepage / Overall length	Form factor	Date energized	Months of test	Number of flashovers	Number of surges	Number of flag operations
	Overall (insulator)	Total	Protected							
	in	in (in/kV)								
Cylindrical support	$63\frac{3}{4}$ ($53\frac{3}{4}$)	113 (0·86)	40 (0·30)	1·77	2·4	March 1944	29	0	80	0
						November 1946	38	0	16 000	0
Torque	$57\frac{3}{4}$ ($53\frac{5}{8}$)	92 (0·7)	21 (0·16)	1·59	6·5	1948		3		
						May 1949	~8			
Torque	$57\frac{3}{4}$ ($53\frac{5}{8}$)	116 (0·88)	57 (0·43)	2·01	8·3	May 1949	124	0	3 970	0
Solid core support	57 ($51\frac{1}{2}$)	104 (0·79)	30 (0·23)	1·83	5·6	March 1952	12	2	7 768	
							12	0	5 560	
						October 1953	71	0	8 843	2

information; the form factor $\int_0^s \frac{ds}{\pi D}$, s being the creepage length and D the diameter, multiplied by the resistivity of the pollution film, would give the surface resistance assuming the film were uniform. Reverey* suggested that the product of total creepage distance and form factor is a measure of performance in salt-pollution conditions.

The first and third insulators are considered satisfactory from the tests and have been found so in service in an area of salt and industrial pollution. They do not altogether meet the creepage distances. The second was not satisfactory on test, and, in fact, one of these insulators flashed over in service after a heavy salt storm. The merits of the last insulator for outdoor use are doubtful in heavy industrial pollution.

I believe that a great deal more work should be done on the factors affecting pollution performance. Attention should also be given to related problems such as the internal performance of hollow insulators and the shed requirements of indoor insulators.

Mr. T. H. Pegg (*at Manchester*): The paper gives insufficient information on live washing, no reference being made to the voltage used on the tests, although from the description of the insulators it is 132 kV.

The statement that 'certain well-established conditions regarding water resistivity and safe distances' could with advantage be amplified since it has now become necessary to consider the live cleaning of 275 kV substation insulators in polluted areas. There are economic reasons for this step, namely high out-of-merit running costs together with decreased security of supply. Substation circuit outage time for essential maintenance of equipment is doubled when hand cleaning of insulators is employed.

The greasing of 275 kV substation insulators has been considered and rejected, since the circuit outage time to apply the grease and to remove it is considerable, and the effective life of the treatment is not considered long enough to merit this method.

The alternative is live washing, and from preliminary tests it appears that the greater portion of the substation insulation can be cleaned in this way.

Dr. J. S. Forrest, and Messrs. P. J. Lambeth and D. F. Oakeshott (*in reply*): It is difficult to determine how far inland the effects of salt pollution will be felt. Flashovers have occurred many miles from the sea, but the limit of 5 miles mentioned by Mr. Qualtrough is rarely exceeded. We know of no evidence that shoal waters make matters worse.

We do not consider that oil-bath insulators are very satisfactory owing to the maintenance needed to prevent water and dirt getting into the bath. Unlike greased insulators it is not easy to see when maintenance is required. In view of the high rate of dirt deposit, we should advise Mr. Pirie against the use of oil-bath insulators near cement works.

Both Messrs. Qualtrough and Spence suggest testing insulators at twice the normal service voltage, but for a comparison of insulator designs it might well give misleading results to use any voltage other than the maximum working voltage.

To increase the range of tests at Croydon, a 33 kV gantry has been installed, and we hope the results of tests at this voltage will be of benefit to distribution engineers. Mr. Braid concludes from our results that rod insulators behave better than disc insulators at lower voltages. We are not sure that this is so. We pointed out in the paper that the results of a comparison between 132 kV anti-fog rod and disc insulators were inconclusive owing to the anomalous flashover of a 9-unit anti-fog disc insulator. Artificial pollution tests have since shown that the anti-fog disc insulator is significantly better than the anti-fog rod insulator in salt-storm conditions and in saline fog.

The paper by Lafont, to which Dr. Alston refers, describes successful tests on epoxy-resin insulators. These are not particularly severe for outdoor insulation, and the test duration was only 50 hours. This paper does not therefore modify our views on the use of plastics out of doors.

Although sparking phenomena on a.c. and d.c. insulators have marked similarities which were mentioned in Section 5.2, there are some important differences which could account for the observed results. For example, the rate of dirt deposition on a d.c. insulator is greater than on an a.c. one, and the alternating capacitive current helps to dry the pollution layer.

* REVEREY, G.: 'Hochspannungsisolatoren unter Fremdschichteinfluss', *Elektrizitätswirtschaft*, 1959, 58, Nos. 2 and 3.

The resistance of semiconducting glaze insulators has been recorded, and as can be seen from Fig. 6, the current may considerably exceed the normal value during rain. Under these conditions the pollution resistance may well be less than that of the glaze, but when dry bands form, their resistance cannot exceed that of the glaze.

The current in the glaze raises the surface temperature by only 1–2° C, and it is not advisable to increase the temperature rise significantly, for the reasons given in the paper.

Since the paper was written more test results have been obtained with the high-capacitance glass insulators, which confirm the favourable impression given by the initial tests.

The corrosion of insulator pins is not a serious problem in this country, but where it has occurred pollution flashovers have also been experienced. In clean areas severe corrosion has only occurred when some fault in manufacture has been found.

The atmospheric conditions mentioned by Mr. Birch may well have been most severe near ground level; this is most characteristic of salt storms, but it also applies to fog and may account for the difference in performance of substation and line insulators.

For suspension insulators to work satisfactorily at 380 kV a minimum of 400 in leakage path is essential, and a figure of 480 in would be necessary to give the same leakage path per kilovolt as on the 132 kV Grid system. We have conducted no tests on 500 kV insulators yet, but we expect that proportionate leakage distances will be required.

The plain insulator shapes mentioned by Mr. Tee were among the first tried (Reference 2 of the paper). They failed because there were no sheds to break up the water stream during rain; even a greased insulator requires some form of shedding.

The behaviour of a polluted insulator when subjected to surges is not of great practical importance since lightning rarely occurs at the same time as the weather conditions responsible for pollution flashovers. In fact, we know of no flashovers due to surges in conjunction with severe pollution conditions. The reduced withstand alternating voltage caused by pollution is largely the result of thermal processes, and therefore the percentage reduction in impulse withstand voltage due to this cause will not be as great as the reduction in the withstand alternating voltage.

The cathode-ray oscillograph mentioned by Mr. Edwards is only one of several types of instrument used for recording leakage currents. Space limitations prevented us from elaborating on the current waveforms obtained, several examples of which are shown in Fig. 13 of the paper.

Mr. Spence rightly emphasizes the cost of preventing pollution faults, but an assessment of the economics of this problem is exceedingly difficult. It should be remembered that even doubling the insulation would not be a guarantee of complete immunity against any conceivable weather condition, and that increasing the insulation level by a substantial amount is not merely a question of adding a few units to existing insulators but involves new tower and substation designs, as Mr. Wood has pointed out.

The artificial salt-storm test simulates the conditions during an actual storm, but we are proposing to reproduce the condition Mr. Spence mentioned (light drizzle on salty insulators) at a later date.

Grease fails to give lasting protection to semiconducting glaze, possibly owing to imperfections in the grease layer.

Dr. Maass is correct in assuming that our mention of a 'figure of merit' implies no relaxation in minimum leakage-path-length requirements.

Mr. Pegg mentions live washing of 275 kV substations, and we congratulate him on the progress he has made in devising apparatus for carrying out this procedure safely.

DISCUSSION ON 'FIELD SUPPRESSION OF TURBO-ALTERNATORS'*

NORTH-EASTERN CENTRE, AT NEWCASTLE UPON TYNE, 25TH JANUARY, 1960

Mr. R. A. Hore: I agree that the suppression voltage (appearing across the slip rings) should be as high as possible consistent with the safety of the rotor winding, and that the arcing time of the circuit-breaker should be short.

Field suppression by the use of the exciter is slower than that which can be achieved by using a circuit-breaker. The suppression voltage builds up more slowly and has a peak value of, say, 1 kV (limited to the exciter ceiling voltage), whereas that achieved across a discharge resistance can be approximately 3 kV and appears immediately the circuit-breaker clears, thereafter decaying exponentially (Fig. A). An arrangement which can make the best of both worlds is shown in principle in Fig. B. This has obvious additional advantages where multi-break circuit-breakers are employed; the duty required of the circuit-breaker is, however, of the same order as that when employing the more conventional circuit. When considering claims put forward for suppression using the exciter, one should be sure that the results quoted are for short-circuit conditions; the spectacular results quoted are for open-circuit conditions.

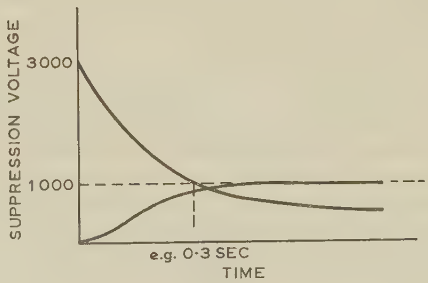

Fig. A

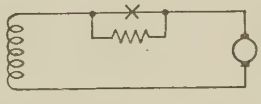

Fig. B

* Hill, J. R., Hunt, A., Joyce, W. J., and Tompsett, D. H.: Paper No. 3161 S, November, 1959 (see 107 A, p. 141).

The authors' estimate of damage assumes that the fault is at the line end of the stator winding. With faults further down the winding, the proportion of fault current supplied by the machine will be greater; the area (*a*) of Fig. 5 will be smaller and consequently there is a somewhat greater advantage in higher discharge resistance than is indicated by Fig. 6.

The authors neglect the effect of rotor amortisseur circuits on the recovery voltage [cf. expression (9) and Section 15.3]. The effect is important in turbo-type machines, especially with high discharge resistances, but not in hydro-machines. This effect would have been indicated if the authors had included calculated field voltages in Table 2, and I would ask them to give these figures based on calculated and on test values of field current at time of arc extinction. If the damping winding effect is not neglected, the component $R_d I_f$ and the value of R_t (Section 10.1) as given by the authors require modification (cf. Reference 2).

My experience indicates that, with care and judicious allowance for saturation, calculated values of the direct components of current and voltage are 115% to 95% of the test values. This fact and my belief that the effect of the a.c. component is not very significant lead me to prefer the direct synthetic test to the authors' method employing a factor *M*, which can hardly be accurately determined. (In fact, the authors also appear to neglect the a.c. component in this context.) The direct test is cheap and practicable and affords a safety margin. The number of field circuit-breakers to be used cannot pay for the tests required to reduce this margin.

Mr. A. H. L. Carroll: The discharge resistance for large generators should be a maximum consistent with a 1 min test of the rotor (usually not less than 3 kV) and breaking capacity of the field circuit-breaker. (My company's circuit-breakers have successfully interrupted 20 kA at 3 kV applied.) I find that this results in a discharge resistance rating of 2·5–4 times the hot field resistance.

Section 6 states that equipment associated with the field circuit is tested at only 2 kV. I advocate my company's policy of providing, instead of direct-connected, current and voltage transductor-operated instruments for generators above 75 MVA rating.

The authors do not mention the provision of protection against slip-ring short-circuit, and I feel that this has been overdue for some time, particularly considering the increase in generator size and consequent losses due to prolonged outage, and also the introduction of the a.c. rectifier excitation scheme.

My company now recommends, for all turbo-generators of 30 MW and over, over-current protection in the form of an instantaneous direct-acting release on the field circuit-breaker, which must be set to trip above the value of the peak current induced by the stator short-circuit.

Fault clearing should be rapid, and for generators above 250 MVA, for example, the suggested time from fault occurrence to arc extinction should not exceed 30 millisec. Naturally, the field circuit-breaker must be suitable for this demand.

Protection against loss of excitation was omitted. I feel that turbo-generators should not be permitted to run asynchronously, and therefore some form of under-current relay in the excitation circuit, arranged to trip the main h.v. circuit-breaker in addition to other necessary tripping, is recommended.

Regarding the synthetic testing of breakers, I submit that the voltage applied should equal the sum of the direct and peak alternating components, while the current should equal the d.c. component only.

Messrs. J. R. Hill, A. Hunt, W. J. Joyce, and D. H. Tompsett (*in reply*): We agree with Mr. Hore's comment that the proportion of the fault current supplied by the machine will be greater if the fault occurs part way down the phase from the line end. Under this condition, however, the total fault current will be reduced.

We confirm his observation that the effect of the damper circuits has been largely neglected in the simplification that leads to expression (9) for the recovery voltage, although it is allowed for in the expressions (11), etc., for rotor current before the circuit-breaker begins to open. With acceptable circuit-breaker arcing times, the time interval between the initiation of field suppression and the peak recovery voltage is very short, and it is our opinion that the damper circuit effect during this period is negligible, especially since an analysis of oscillograms taken on a particular alternator using different values of discharge resistance indicates that the amortisseur had a resistance of six to nine times R_f.

While we agree that, in principle, the magnitude of the amortisseur effect could be found by comparing calculated and measured recovery voltages, we fear that the calculation would not be of sufficient accuracy to allow the damper effect to be separated with any certainty. Some reasons for this inaccuracy are given in Section 7 of the paper.

All our test results show that the final interruption of current through the circuit-breaker occurs at or very near the current trough, as in Fig. 3, and it is clear that the circuit-breaker must then withstand the voltage occurring at the next peak of current in the winding and discharge resistor. Any test should therefore subject the circuit-breaker to a voltage equal to this peak, and in order to approach more nearly to service conditions we have done tests with an unsmoothed rectified a.c. supply of appropriate voltage. If a purely d.c. test is to be made, we prefer Mr. Carroll's proposal that the voltage applied should equal the estimated peak value, allowing for the a.c. component.

We note that Mr. Carroll advocates transductor-operated instruments, but we feel that the extra cost and complication is not justified with the limits of peak field voltage proposed at present.

We support his view that protection against rotor circuit faults should be provided on large alternators, and this is being done on machines of our company's manufacture.

In a.c. fed excitation circuits, each chain of rectifier cells is protected by a high-speed fuse, irrespective of the circuit-breaker tripping arrangements.

It is certainly true that a turbo-rotor may be damaged very quickly by asynchronous operation at more than a fraction—say half or less—of full load, and protection against loss of excitation is nowadays normally provided. Some operators prefer to give alarm only, but with modern highly rated sets of 100 MW and above there is so little time in which to reduce load before damage occurs that automatic tripping, with enough delay to ensure that the steam valves are fully shut, is the only practicable course.

DISCUSSION ON

'GENERATOR/MOTOR PROBLEMS IN PUMPED-STORAGE INSTALLATIONS'*

Before the NORTHERN IRELAND CENTRE *at* BELFAST 12*th January, and a* JOINT MEETING *of the* SOUTH MIDLAND CENTRE *and* SUPPLY AND UTILIZATION GROUP *at* BIRMINGHAM 4*th April*, 1960.

Mr. W. Szwander (*at Belfast*): On the Continent numerous pumped-storage installations have been in use for a considerable time, but in this country the awakening of interest dates only a few years back, and was brought about chiefly by the advent of nuclear generation, though the advantages which pumped storage can bring in conjunction with conventional generation are now being more and more appreciated. In Northern Ireland it is likely that construction of a sizable pumped-storage installation will commence soon, this being the first step towards nuclear generation, which is impracticable here without pumped storage on account of the low system load factor and minimum night load. The desire to avoid any extension of conventional generation comes from the fact that both coal and oil are imported fuels, suffering the disadvantages of high cost and insecurity of supply. On the other hand we are fortunate to have excellent high-head sites situated near the load centre and allowing very economical development of pumped storage which will be advantageous, even for operation in conjunction with our existing conventional base-load stations.

Reversible pump/turbines undoubtedly will find use in installations with hydraulic heads up to at least 1 000 ft, because of considerable first-cost savings on the civil engineering and plant sides, but it is important to appreciate the following limiting factors. While, in a set in which the pump and the turbine are separate machines, the input to the motor can be controlled by operating the turbine and the pump in parallel and controlling the water flow through the turbine, in the case of a combined reversible pump/turbine unit for high heads when machines with adjustable blades (Deriaz type) cannot be used, the power input to the pump is constant. When the capacity of the pumped-storage installation is not very small relative to the size of the system, sufficiently fine adjustment of the pumping load to the power available for pumping is required, and this, with reversible pump/turbines, will be possible only by dividing the total capacity of the pumped-storage installation into a number of sufficiently small units, thus increasing the first cost. Furthermore, direct-on-line starting is possible only in exceptional cases, while other forms involve extra cost and lengthen the starting time; this may be not acceptable when rapid availability of capacity is important for immediate-standby plant—one of the important economic justifications of pumped storage.

The 200 MW 150 r.p.m. machine discussed in the paper would be suitable for a hydraulic head of the order of 400 ft. This, while feasible, is not typical for pumped-storage installations, which are much more economical for higher heads, and fortunately heads of the order of 1 000 ft are not difficult to find (Ffestiniog, Loch Awe, Northern Ireland). Also, the size of the machine appears exaggerated. The total capacity of a scheme is always limited by site considerations, and for reasons of operational flexibility subdivision of this total capacity into a number of units is essential. Thus, the tremendous growth of unit sizes experienced nowadays in the field of steam turbines cannot be taken as a precedent for hydro-generation, including pumped-storage installations.

* WALKER, J. H.: Paper No. 2853 S, February, 1959 (see 107 A, p. 157).

Mr. J. T. Irwin (*at Belfast*): Pumped storage in Northern Ireland should differ from that visualized where the pump and the turbine are of comparable size. A beneficent Providence endows us with a high rainfall and a good flow from moderately high hills. The sun and the Gulf Stream do most of the pumping, and with a high-level reservoir the pump is only required to firm the peak-load output of the turbine. This pump would be at most one-fifth of the output of the turbine.

A trial scheme appears to give a peak power of 48 MW and a yearly output of 10^5 MWh. It would not help much in absorbing surplus power from a nuclear station, which could be more efficiently absorbed by thermal storage on consumers' premises if encouraged by a suitable night-load tariff.

Mr. H. M. Fricke (*at Birmingham*): Is the normal winding of a double-wound motor left open-circuited, and, if so, what voltages are generated in it? Can the author give figures of centrifugal forces in the rotors of the massive machines which have been described?

Mr. H. Foster-Smith (*at Birmingham*): The provision of cheap electrical power in under-developed countries sometimes necessitates the installation of a small, simple and robust hydro-electric plant to be operated by unskilled personnel in very primitive country. In order to achieve the essential simplicity of operation, generating sets are fitted with a water brake which operates automatically as the generator load is reduced, thus maintaining a constant load and gate-opening on the water turbine. Alternatively, this artificial loading may be by magnetic brake or by resistances. Would it be possible to design a very simple equipment to utilize this wasted energy for pumped storage?

In the small generating sets the pump could take the place of the water brake on the main shaft, but in larger plants of two or more sets it is thought that one set could be designed as an induction generator/motor driven by or driving a turbine/pump. What is the largest size of set which could be installed under these conditions? The induction generator/motor is suggested because of its operating simplicity and because, in the isolated plants under consideration, direct starting would not cause the inconvenience it does in more sophisticated communities. The plant should be electrically automatic, receiving only periodic inspection from a competent engineer.

The author's opinion is also sought on the type of equipment, and the possibilities of utilizing pumped-storage plant in unattended power stations situated on remote sites. The power stations would operate under heads of 100 or 300 ft, and the capacity would vary from 1 to 30 MVA, it being understood that high performance efficiency is secondary to robust design and extreme simplicity of operation. The smaller plants would be independently run, and the daily load curve that of a rural district with a low load factor. The larger plants would eventually be connected to a system, but for some years would be operated independently.

Mr. V. Easton (*at Birmingham*): An addition to the list of references is a paper by Tittel.* A similar method of pole

* TITTEL, J.: 'Variable Speed Synchronous Machines for Hydroelectric or Pumped Storage Power Station', C.I.G.R.E., Paris, 1954, Paper No. 109.

changing for the rotor is described, and test figures on a 450 kVA 18/22-pole experimental alternator are presented, with several oscillograms. The latter are most interesting in demonstrating that the correct design of stator windings is capable of producing a substantially sinusoidal voltage from the distorted m.m.f. and flux distribution equivalent to Fig. 1(*b*). In this machine the iron losses for normal voltage at the two speeds were comparable, but the short-circuit stray loss was over 150% greater with the abnormal number of poles. This is a serious disadvantage if a similar effect applies to larger units operating on commercial load. It is advantageous to arrange for the actual number of poles to correspond to the generating condition, as the higher speed when motoring then tends to compensate for the lower effective flux with the reconnected rotor windings.

An alternative method of starting not mentioned in the paper is to use a reactor in the neutral point of the stator windings, when it should be possible to replace the costly heavy-duty circuit-breakers with remote-operated isolators. An even more attractive method would be to use a saturable reactor of the transductor type such as has been built and operated very satisfactorily to provide artificial load for works tests on large turbo-alternators.* The controlled variation of impedance then possible would allow a very low inrush current on starting with a build-up over a period of several seconds so that the system voltage could be maintained by a.v.r. operation on other machines.

Since separately driven ventilating fans are undoubtedly the most suitable arrangement for bidirectional generator/motor units, the comparative figures given in Section 9 appear needlessly harsh on the straight-bladed fan. If the average values of the range in efficiency quoted for the aerofoil and straight-bladed fans are considered, the ratio of the loss for the two types is much below six, whilst, in the last paragraph, if an efficiency of 15% instead of 10% is assumed the quoted saving in loss is nearly halved. Separate fans also permit circulation of cooling air whilst the set is braked to standstill and would appreciably reduce the excess temperature rises quoted in the last column of Table 1.

Mr. J. Terry (*at Birmingham*): Can the author give an idea of the overall economics of the system? What is the efficiency of the complete cycle, and what is the optimum ratio of generating power to pumping power?

* Easton, V., Fisher, F. J., and Friedlander, E.: 'A 100 MVA Transductor for Testing Alternators', C.I.G.R.É., Paris, 1958, Paper No. 117.

Dr. J. H. Walker (*in reply*):

To Mr. Szwander.—Although it is correct to say that a rating of 200 MW at 150 r.p.m. is not typical of pumped-storage units at present under construction or projected, there are schemes under investigation which envisage units of this size and speed.

To Mr. Fricke.—In a double-wound motor the winding not in use is left open-circuited, since, although no fundamental e.m.f. is generated in it, harmonics might produce appreciable currents if it were short-circuited.

On a 200 MW 150 r.p.m. generator the centrifugal force produced by each pole is about 450 tons at rated speed and 1 450 tons at overspeed, so that the total centrifugal force acting on the rotor rim due to the poles alone is 18 000 tons at rated speed and 58 000 tons at overspeed.

To Mr. Foster-Smith.—The applications of pumped-storage suggested here are certainly feasible technically, but for the low ratings mentioned the main objection would be the very high cost per kilovolt-ampere of installed capacity. Automatic operation offers no technical problems, but here again cost would require careful consideration.

In general, induction generator/motor units can be built for ratings comparable with those of synchronous machines. In practice, however, the size of such induction machines may be restricted by the ability of the system to supply the required magnetizing current, though this limitation could, in some cases, be avoided by the use of static capacitors.

To Mr. Easton.—The very high short-circuit loss in change-pole synchronous machines is due partly to the asymmetry of the stator windings on successive poles and partly to the disconnection of certain rotor poles. This loss alone would appear to preclude the use of change-pole machines for large outputs, but investigations into alternative methods of changing the rotor m.m.f. pattern to produce the change in speed would indicate that this undesirable feature can be largely eliminated.

To Mr. Terry.—The overall efficiency of a large pumped-storage station is about 66–68%. Economics and the ratio of generation to pumping are discussed in References 1, 3 and 5.

DISCUSSION ON 'THE CHARACTERISTICS AND PROTECTION OF SEMICONDUCTOR RECTIFIERS'*

Before the North-Western Utilization Group *at* Manchester, *12th January, the* North-Eastern Centre *at* Newcastle upon Tyne, *22nd February, the* Tees-Side Sub-Centre *at* Middlesbrough, *6th April, and the* North Staffordshire Sub-Centre *at* Stafford, *16th May*, 1960.

Mr. C. A. M. Thornton (*at Manchester*): An application of semiconductor rectifiers likely to become important is that of inverters for feeding variable-frequency square-wave voltages to squirrel-cage induction motors, the frequency being varied over a range of up to 5 : 1 by variation of the direct voltage applied to the inverter.† The current wave taken by the motor is peaky and the peak limits the output of the rectifier. The authors deal with sinusoidal and square waves but not with peaky waves, and I should like their comments on this case. Is it possible to protect effectively a rectifier loaded in this way with a suitably designed fuse, and may we hope to see the system applied to motors up to, say, 25 h.p. at 440 volts a.c.?

The authors say nothing about national and international work on standardization of the protection of semiconductor rectifiers. Preliminary work was done by an I.E.C. committee in June, 1959; has any noteworthy progress been made since then?

Examining a newly supplied semiconductor rectifier welder, I found that it was protected by fuses to B.S. 88, and presume that this protection is not sufficiently close. No doubt many rectifiers of this type are now being protected by standard industrial fuses cooled in the forced draught of air used to cool the rectifier elements. Could this feature be used to improve the protection given by the fuses?

* Corbyn, D. B., and Potter, N. L.: Paper No. 3135 U, November, 1959 (see 107 A, p. 255).

† Card, W. H.: 'Transistor Inverter Drives Induction Motor', *Electronics*, 1959, 32, p. 60.

Section 4.1 refers to small resistors used to encourage the correct distribution of reverse voltage. Will the authors state the resistances and current ratings they would recommend, together with losses involved by the use of these resistances?

What practical variations are obtained between cell characteristics producing bad current-sharing between parallel cells? I believe that automatic control of crystal formation is now employed to achieve uniformity so far as possible. Has this improved the cell characteristics so far as current sharing is concerned?

How wide a range of breakdown voltage can be produced, and is any special technique employed in determining the breakdown voltage without damage to the cell?

Section 2.1 refers to 'reverse ageing of junctions with time'. Does the ageing refer to a decrease of breakdown voltage or an alteration in the regulation? The expression 'reverse ageing' may mean 'ageing of the reverse voltage curve'. Could the authors explain the term?

Section 9.1 states that 'satisfactory discrimination can be obtained if the mercury-arc rectifier is fitted with anode fuses'. Would fuses supplied for normal use by manufacturers as anode fuses be satisfactory for this purpose or should special fuses be used in the anode-fuse position?

Motor flashovers on traction systems may be caused by faulty operation of control gear. High-speed circuit-breakers are therefore presumably required in any case. Does this fact ease the design of fuse for a rectifier supplying a traction load?

Mr. E. W. Sugden (*at Manchester*): My company produces fuses for this duty in co-operation with leading manufacturers of rectifiers, but the fault capacities of up to 200kA accessible to the authors were not available to us. However, the use of relatively small fuses in parallel to protect individual strings of diodes is a more advisable solution, and with these a condition of reduced prospective current, as envisaged in B.S. 88: 1952, is both the more severe and the one most likely to excite high over-voltages during the arcing period of the fuse. This is why the upper arc-voltage limits of 2·5kV for fuses above 10amp rating and 3kV for those below 10amp, as a criterion of failure, are confined to tests at full prospective current.

My belief in the use of relatively small fuses in parallel is based partly on a consideration of the data provided in the paper, and perhaps to a greater extent on the thought that such a course is fundamentally a correct way to secure small thermal time-constants in the sensitive part of the fuse element which operates under severe fault conditions. This thermal time-constant is of the most vital importance and almost wholly determines the I^2t admitted by the fuse during the pre-arcing period. Data have been published oversea indicating that fuses with current ratings up to 5kA are feasible and consistent with satisfactory current-limiting properties, but I think that this belief is ill-founded, since the thermal time-constant of fuses increases with size.

My supposition that the condition of reduced prospective current is the more relevant is based on the fact that the prospective current producing the maximum arc energy in a fuse is usually very similar to that which produces the maximum inductive energy, $\frac{1}{2}LI^2$, developed in the circuit before the onset of arcing. This condition is secured by the requirements of Test 21(*b*) of B.S. 88. If this conclusion is justified, tests at reduced prospective current would give the best possible data on fuses for the protection of semiconductor diodes.

For comparison with Table 1, the following figures were obtained from tests under the conditions envisaged by Test 21(*b*) of B.S. 88:

Current rating: 75amp.
Fusing factor: Under 1·25.
Voltage of tests: 450 volts r.m.s. (i.e. 637 volts peak).
Maximum $\int I^2dt$ obtained on tests, in accordance with Clause 21(*b*) B.S. 88: 6240amp²sec.
Maximum arc voltage obtained on tests: 560 volts.
Power factor during tests: 0·15.

These fuses are intended for use on the a.c. side of the rectifiers to protect series strings of diodes where the r.m.s. supply voltage does not exceed 440 volts, and a range of sizes has been produced from about 20 to about 250amp, i.e. from the smallest which it is mechanically feasible to make to the largest which we consider suitable for use as a single unit.

Mr. F. H. Lomas (*at Manchester*): With the present move towards a.c. electrification of the railways, semiconductor rectifiers promise to have a big application. An understanding of the rectifier characteristics is essential for the study of protection, and I should like to know how close to the survival limits shown in Fig. 3, for example, one may work in practice. Is some damage sustained each time a survival limit is approached?

In a single-phase traction bridge circuit with a 10% reactance transformer and a motor load of 2kV at 400amp, a backfire in a rectifier cell will give approximately 20 times the normal rectifier load current and the special fuses will provide the necessary protection. Now, in the event of motor flashover, fuses must not blow and the necessary protection must be provided by a circuit-breaker. Many circuit-breakers in use to-day have been designed for larger equipments and have operating times of 60–100millisec. Unless a circuit is over-rated or has high reactance, the average circuit-breaker may be too slow for adequate protection. I suggest that the use of high-speed circuit-breakers operating in 20millisec offers a chance of economy in the use of rectifier cells and/or reactance.

I wish more had been said about the use of reactance. Most manufacturers at present are recommending values of 10–15%. It is evident from Fig. 5(*a*) that the use of long series strings helps in current sharing. As cell characteristics improve, these series strings should become shorter, and I think that the use of balancing reactors is worth further study. In this respect it is interesting to note that Ogden* showed that distribution of the main reactance in this way can give the required protection against backfire and also a useful reduction in reactance voltage losses.

Mr. R. S. Paulden (*at Manchester*): Fig. 2 indicates the variation of junction temperature over a complete cycle; is it found necessary, because of this cyclic variation, to reduce the mean current rating of cells working on very-low-frequency circuits, such as slip-energy-recovery systems?

For the series-parallel connection of cells, Fig. 5(*a*) shows the favoured arrangement. In Fig. 5(*b*), with an extra cell group in circuit and one group short-circuited by a faulty cell, the equipment may still not be really safe for continuous operation; the current distribution would presumably be upset by the short-circuited cell and there may be overheating in some of the connections.

My experience of voltage surges indicates that the greatest hazard is from transformer switching with an unloaded rectifier, but it is usually possible to limit these to 1½ times the recurrent peak inverse voltage by quite a moderate capacitance in the primary winding. This fixes the rated peak inverse voltage of the rectifier cell at about 1½–2 times the recurrent working value. Does the authors' experience confirm this as a reasonable compromise between rectifier and surge-capacitor costs?

Fig. 11 indicates three different positions for surge-suppression capacitors, and Section 13.3 indicates that all these can be replaced by a single *RC* filter in the d.c. output. This would

* Ogden, H. S.: 'Considerations in the Development of a High-Power Rectifier Locomotive', *Transactions of the American I.E.E.*, 1955, 74, Part II, p. 169.

lead to a considerable simplification and allow the use of an electrolytic capacitor, but my experience is that transformer-switching surges are more effectively suppressed on the primary.

There are some rectifier conditions where normal industrial-type h.r.c. fuses are suitable. They may be necessary in units with many parallel rectifier cells under-run to withstand repeated short-circuits. The fuses chosen must not blow on momentary short-circuits, and because of the large number of parallel cells, protection against a faulty cell is not very difficult.

Mr. R. M. Rear (*at Manchester*): It is stated that, in order to prevent the spread of damage, it is necessary to disconnect a faulty cell or string within 5 millisec, but the characteristics of fuses show operation in 5 millisec or thereabouts only at the very minima. Is it therefore always necessary to choose fuses so that this point can be attained, or is there a latitude in the clearance time under reduced-fault-current conditions which permits the time to be increased? Is this choice of fuse affected when a semiconductor rectifier is operated in parallel with, say, a contact rectifier, which is liable to backfire with consequent operation of a short-circuiter and thus submit the semiconductor rectifier to severe effectively-d.c. short-circuits until the contact-rectifier d.c. circuit-breaker opens?

The level at which the instantaneous element is set, $3\frac{1}{2}$–4 times nominal, appears somewhat arbitrary and would demand measures to prevent maloperation under transformer magnetizing-current inrush conditions. With this range of settings some form of time-lag would appear to be the only feasible method, but unfortunately this would not discriminate between fault conditions and magnetizing-current inrush. What is the reasoning behind the adoption of the setting of $3\frac{1}{2}$–4 times nominal, and would it be feasible to raise it to, say, 6 times to obtain faster tripping and yet preserve stability to inrush currents?

There would appear to be excessive caution in the application of the i.d.m.t.l. relay to the low-level overloads, as shown in Fig. 10, yet permitting a lack of discrimination between fuses and relays over the instantaneous element and higher setting end of the i.d.m.t.l. relay characteristics. It would be expected that better discrimination would be required between the fuse and the instantaneous element to ensure that fuse blowing was prevented, except under cell-failure conditions, and by the adoption of a more inverse i.d.m.t.l relay characteristic to make the relay characteristic follow more closely the rectifier and fuse characteristics. However, the conventional i.d.m.t.l. relay is not accurate below 130% of setting, whereas it is believed that for the adequate protection of semiconductor rectifiers it is necessary to retain accuracy down to 110–115% of setting. The authors' experience in this respect would therefore be welcomed.

Mr. J. C. Jones (*at Manchester*): In view of the time which elapsed before copper oxide and selenium rectifiers were accepted for general use in industry, can the authors assess the probable life of germanium and silicon cells in service?

Do the authors consider that semiconductor rectifiers will supply variable-speed d.c. motors satisfactorily when driving regenerative types of load where sudden changes of field strength may give rise to over-voltages on the d.c. side?

No mention is made in the paper of the relative costs of semiconductor and mercury-arc rectifiers. It is my impression that semiconductor rectifiers are more economical in first cost for output voltages up to 220 volts at, say, 40 kW rating, but not for higher direct voltages. Will the authors comment on this?

Mr. K. J. H. Thomson (*at Manchester*): Section 9.4 states that a short-circuit on the rectifier output when feeding excitation to an alternator field is so rare that protection against it is not required. Experience shows that slip-ring short-circuits are extremely unlikely to occur in service, particularly with the latest large-collector designs. Nevertheless, power authorities at present tend to ask for protection against such a fault when considering rectified excitation. If, for this and other reasons, a d.c. field circuit-breaker is included, the rectifiers and fuses must be designed to carry the through fault current until the circuit-breaker clears the fault; it is also desirable to include high reactance in the a.c. circuit feeding the rectifiers to limit the fault current. If, however, a field circuit-breaker is not included, the only degree of protection is that given by the fuses in the parallel branches of the rectifiers. (Opening the exciter field would reduce the fault current, but only after a relatively long interval.) Can present-day fuses be expected to perform this duty?

Mr. J. N. M. Legate (*at Manchester*): Will the authors comment on the practical necessity for full protection? For example, an apparently simple rectifier bank may become a rather complex piece of apparatus when an *RC* network for hole-storage protection, resistance chains for voltage sharing and possibly reactors for load sharing are incorporated. The additional complication would tend to reduce the acceptability of semiconductor devices to many industrial users, and I wonder whether a considerable amount of complication can be avoided by judicious derating or selection of the rectifier units.

Mr. D. O. Heinrich (*at Manchester*): I am interested in the application of semiconductor rectifiers for high-voltage low-current applications, and should like the authors' comments with regard to these applications, in particular when using silicon diodes for such purposes.

Mr. F. L. Hamilton (*at Newcastle upon Tyne*): Many rectifier failures have been due to incorrect application or unforeseen voltage or current conditions in the circuit, but it has not been easy to obtain information other than the recommended continuous ratings of voltage and current, which are usually inadequate for most circuit applications. Presumably, the much reduced margin between safe working and destruction has now made additional information essential, and the authors' conditions for the protection of diodes are necessary for their correct application.

Because the reliability of components is so important, I feel that the diode manufacturers have carried the high continuous rating of their components too far and that the margins between normal running and total destruction are small. How far does the close matching of diode characteristics and protection characteristics take into account the normal scatter and variations one might expect in both types of equipment?

The fuse is the most natural protective element for the diodes, since both respond to thermal effects (although somewhat differently), and it is feasible to co-ordinate such protection with the remainder of the protective apparatus. It is possible that the standards of selectivity and reliability for protection in the present applications of d.c. power supplies are not quite so exacting as those on a.c. power systems, thus easing the problem.

Some of the desirable fuse characteristics listed by the authors can be met with standard fuses, but special fuses are desirable for the required overall characteristics. Is the particular characteristic of limited arc voltage in the fuse not satisfied by the over-voltage protection equipment provided in the circuit, and how is the limitation of arc voltage obtained in the fuse? One method might be to grade the fuse-element dimensions to secure progressive elemental arcing along the fuse length.

Mr. R. Headey (*at Middlesbrough*): Is there at present a case for using semiconductors for battery charging, since reliability is more important than efficiency when charging electric trucks working on a 24-hour duty cycle? It would appear that a failure is more likely with the semiconductor rectifier than with the older type of metal rectifier, and while it has been shown that the equipment can be adequately protected by means of fuses, a

blown fuse could remain undetected until the battery was required for service. This would result in an uncharged or partly charged battery, and hence a truck out of action for part, or the whole, of a shift.

Is the apparent proneness to failure due to lack of experience in producing the junction, and is this likely to be overcome in the near future?

The germanium junction is destroyed at 105° C, but no sharp temperature limit is known for silicon rectifiers; it would thus seem that there is no case for using germanium. What are the advantages and disadvantages of the two types?

Special h.r.c. fuses have been developed by one company, but could one be sure that they will be fitted to any proprietary semiconductor rectifier?

Since the cell and fuse curves are so closely matched, is it not possible that the deterioration of the cell with time (which is a known feature of metal rectifiers) might lower the cell curve until no protection was afforded by the fuse?

Mr. T. Robertson (*at Middlesbrough*): I recently tried to use some 13 amp germanium diodes in an application where short-circuits would occur from time to time, but found that the maker's maximum-safe-overload/time curve lay on the 'wrong' side of the clearance-time current curve for a 15 amp fuse—the nearest standard for most types of fuse unit. Have quick-clearance fuses been designed or must one limit germanium rectifier outputs, where short-circuits might occur, to meet normally available fuse characteristics?

Mr. J. A. Smith (*at Middlesbrough*): Have the authors had any experience of one cell in a string of, say, four breaking down without a fuse blowing? On my set there is audible warning to indicate that a fuse has blown, normally as a result of cell failure. We have had an alarm as a result of cell breakdown without a fuse blowing.

Why is silicon not used exclusively, because of the much greater Zener voltage at which a rectifier cell will break down on reverse voltage? It appears that the possibility of breakdown due to high reverse voltage is the major factor which must be taken into account in semiconductor rectifier design. I can appreciate that there will be greater loss due to the greater forward-voltage drop, but surely this is more than offset by the greater reliability of the silicon rectifier?

Mr. A. Asbury (*at Stafford*): The idea of a short-circuiter switch as a means of protection is a little strange to engineers who spend much of their time thinking of the inverse process.

Some interesting problems in protection will arise when the rectifiers are supplying large inductive loads such as the fields of mill motors in a tandem mill. The issue may be slightly complicated when the normal and controlled rectifiers are used in series to give an economic controlled supply. What protective measures would the authors advocate when it is essential to maintain the supply through an inductive load?

Mr. A. G. Thomas (*at Stafford*): I should like more information on the symptoms likely to be detected in the rectifier cells after their protective fuse has blown, and to know whether there is any prospect of salvaging any or all of such rectifiers.

I should also like information as to where and how earth connections are commonly made on to the d.c. system fed by rectifiers; if these earth connections are separated by the fuses from the transformer winding, what steps are taken to limit danger caused by break-through between high- and low-voltage windings?

Mr. D. Montgomery (*at Stafford*): The paper and subsequent discussions have emphasized the importance of adequate protection against the consequences of cell failure. Could the authors place this point in perspective by giving figures for cell failure rate in semiconductor equipments?

Mr. J. J. L. Weaver (*at Stafford*): The protection arrangements outlined in the paper appear to be based on rectifier cells failing by short-circuiting. What steps should be taken to cater for cells which fail by open-circuiting?

Will the authors amplify their remarks regarding voltage sharing in series-connected strings of rectifiers, taking into account transient voltage surges?

Messrs. D. B. Corbyn and **N. L. Potter** (*in reply*): *To Mr. Thornton.*—Fuses are applicable to any current waveform and are often forced-draught cooled. Industrial h.r.c. fuses are satisfactory for small high-reactance equipments such as welders, but are unsuitable for semiconductor protection on industrial equipments where cut-off operation of fuses is required. Current sharing and the use of circuit-breakers are considered in our reply to Mr. Lomas. Cell ageing is rare and usually causes increased reverse leakage and sometimes decreased voltage breakdown. Voltage-sharing resistors usually cause less than 0·10% drop in efficiency.

The I.E.C. have now adopted a provisional semiconductor specification to be reviewed after 3 years' use.

We agree with Mr. Sugden in preferring small fuses protecting one or two strings of cells. High rupturing capacity and testing over a very wide range of fault current are essential; and 200 kA is about the maximum possible peak fault current for one rectifier string in any size of equipment.

To Mr. Lomas.—The survival limit shown does not damage rectifier cells and applies to the worst cell leaving the factory. Current-balancing reactors are technically satisfactory, but the economics are doubtful and it is usually preferable to derate and select rectifier cells for parallel operation. The ideal is a uniform mechanized production of cells, but present commercial silicon cells vary from 1·1 to 1·4 volts peak forward drop in the extreme cases of nominally identical cells at full load current. High-speed d.c. circuit-breakers with moderate transformer reactance will prevent the loss of fuses on terminal short-circuits in both industrial and traction equipments. Each case should be treated on its merits.

To Mr. Paulden.—Current rating of cells is usually reduced in very-low-frequency circuits such as slip-energy-recovery equipments. With the connection shown in Fig. 5(*b*) a short-circuited cell has a negligible effect on current sharing. Switching out an unloaded transformer normally produces the worst voltage surges and it is satisfactory to use a single *RC* circuit across the output for bridge connections. Extra protection is necessary in half-wave circuits. The voltage factor of 2 commonly employed is determined partly by experience. It would, in fact, be cheaper to use more surge protection to reduce the voltage factor still further, but when this is achieved there are risks which the designed surge-protection components may not adequately handle and where accurate calculation is difficult. For this reason the factor of 2 is considered the present minimum safe value for industrial equipment.

To Mr. Rear.—The relay settings shown in Fig. 10 are based on the best available i.d.m.t. relays and instantaneous relays stabilized against the inrush of magnetizing currents with typical present-day silicon cells used at full rating. Even more inverse i.d.m.t. relays are under development. For operation in parallel with a mechanical rectifier the short-circuiter frequently offers the cheapest effective protection.

To Mr. Jones.—Semiconductors are safe for supplying regenerative loads, but special protective measures must be taken against the over-voltages produced, and the rectifier manufacturer should be given the fullest possible information at the inquiry stage.

The semiconductor rectifier has already largely superseded the mercury-arc rectifier for all medium voltage applications where

grid control is not essential. The life of well-made modern cells appears to be almost unlimited.

To Mr. Thomson.—Turbo-alternator excitation is recognized as an extremely onerous duty with heavy fault current occurring during a stator short-circuit on the main alternator. This fault must not cause any loss of rectifier fuses. Our experience, based on full-scale tests on a 120 MW alternator, is that present-day rectifier cells and fuses are entirely adequate to meet the needs of this equipment.

To Mr. Legate.—Equipment simplification is most desirable. Protective gear against either over-current or over-voltage is cheap and reliable and its proper use greatly reduces equipment cost without loss of reliability. We fear that our detailed examination of the protection problem in the paper has given a false impression of complexity. In practice, the voltage-surge-suppression components can usually be reduced to a single capacitor and resistor.

To Mr. Heinrich.—The silicon diode is quite satisfactory for high-voltage applications, although special care is necessary to ensure correct voltage division in very long strings.

To Mr. Hamilton.—The safety factors of current and voltage already discussed are found adequate in practice, although greater ones are sometimes desirable in control and protection circuits. Fuse arc voltages are limited by special shaping of the silver elements, and although the voltage-suppression circuits would reduce this arc voltage, it is safer to limit the voltage at source and use the fuse as a last line of defence.

To Mr. Headey.—Modern semiconductors are completely reliable and, provided that the cell is correctly rated, show no sign of deterioration.

Germanium rectifiers have some advantage over silicon on low-voltage equipments where the lower forward drop will mean an increase in efficiency.

To Mr. Robertson.—Special fuses have been developed for large power equipments, but some derating may be necessary on low-power equipments to accommodate existing fuses.

To Mr. Smith.—We have had experience of one cell in a series string breaking down without blowing fuses. This does, of course, mean that the other cells in the string are subjected to a higher inverse voltage.

To Mr. Asbury.—Rectifier protection on inductive loads is not particularly difficult. The over-voltage on the rectifier which occurs during d.c. circuit-breaking is produced by energy stored in the transformer leakage field, not in the load. If silicon controlled rectifiers and diodes are used in series, special measures will be necessary to ensure correct voltage division, and this problem is being studied at the moment.

To Mr. Thomas.—Faulty rectifier cells should be scrapped: salvage is not practicable. Customers' needs dictate the earth point on rectifier equipment outputs, and it is difficult to generalize. High-voltage/low-voltage breakdown on transformers is commonly protected either by earth screens or spark-gaps.

To Mr. Montgomery.—The permissible failure rate of rectifier cells varies with the application. A computer with perhaps 10 000 semiconductor devices requires a very much lower failure rate than a small industrial equipment with less than 100 cells. Many large industrial equipments with thousands of cells have had no cell failures in two or three years' working. A rate of about $\frac{1}{4}\%$ per annum is thought to be the maximum acceptable for industrial purposes. Some early installations are believed to have had about 4% failures per annum, but this is no longer true and it is certainly not an acceptable rate.

To Mr. Weaver.—We have neglected the risk of an open-circuited cell in designing protective systems, since this fault does not occur spontaneously in properly made cells. Transient voltage sharing in very long strings requires special care.

DISCUSSION ON 'THE APPLICATION OF IRRADIATION IN INDUSTRY'*

RUGBY SUB-CENTRE, 9TH MARCH, 1960

Mr. K. F. Orton: The economic factors presented in the paper show quite clearly the advantages of electron beams over γ-radiation for irradiation work. The majority of irradiation work in the United States is conducted with electron accelerators, but it has been reported recently that in the Soviet Union γ-ray sources are used almost exclusively. It would be unfortunate if, in the attempt to find a use for irradiated nuclear fuel, there were any large-scale effort devoted to γ-ray facilities in this country. Apart from the greater capital cost involved in γ-ray installations, the method introduces extremely complex problems of maintenance and inspection compared with the relatively simple electron-accelerator installation.

The paper is concerned primarily with the uses of electron irradiation, but I would like to deal briefly with the effects of irradiation by fast neutrons. In general, this produces only undesirable results. In reactor design one is concerned with the effects of fast-neutron damage on the uranium fuel, the graphite moderator, the steel of which the pressure vessel is made, and all components and equipment which have to go into regions of high fast-neutron flux.

Fast-neutron damage differs from electron damage in that the momentum of a fast neutron is sufficient to dislodge atoms within the crystal structure, giving rise to vacancies and interstitials. The presence of these is invariably deleterious to the material concerned. For example, fast-neutron irradiation raises the temperature at which steels change from a brittle to a ductile state, which means that the designer must ensure that the temperature of a pressure vessel never falls below this transition temperature. Unfortunately there is a grave lack of information about the fundamental aspects of the problem and pessimistic calculations must always be made. It is hoped that the next few years will see a greater understanding of this problem, which is of interest, not only to reactor designers, but to metallurgists and solid-state physicists.

Mr. M. C. Crowley-Milling (*in reply*): I agree with Mr. Orton as to the advantages of electron beams for irradiation, and I understand that, although at one time much was said about the advantages of using irradiated nuclear fuel for commercial irradiation sources, expert opinion has now changed, and the disadvantages are more widely realized.

I would not go as far as Mr. Orton in saying that the effects of fast-neutron irradiation are always undesirable, but I agree that more information on the fundamental effects is needed.

* Crowley-Milling, M. C.: Paper No. 3145 U, October, 1959 (see 107 A, p. 111).

PAPERS AND MONOGRAPHS PUBLISHED INDIVIDUALLY

Summaries are given below of papers and monographs which have been published individually. The papers are free of charge; the price of the monographs is 2s. each (post free). Applications, quoting the serial numbers as well as the authors' names, and accompanied by a remittance where appropriate, should be addressed to the Secretary. For convenience, books of five vouchers, price 10s., can be supplied.

The Influence of Ageing on the Characteristics of Oil-filled Cable Dielectric. Paper No. 3348 S.

P. Gazzana Priaroggia, Dr.Ing., G. L. Palandri, Dr.Ing., and U. A. Pelagatti, Dr.Chem.

An investigation has been made of the electrical and mechanical characteristics of the insulation of samples of cables in the range of 60–230 kV after many years of operation in order to evaluate the influence of ageing on the dielectric of oil-filled cables. To determine the effect of thermal ageing alone and in combination with electrical stress, a long series of laboratory tests has been carried out on components of cable insulation, on cable models and on actual cables. A comparison has been made between the laboratory investigations and the state of the insulation of cables after many years of operation.

The following conclusions have been drawn:

(*a*) Paper is the component most affected by temperature.

(*b*) The electric strength is not influenced, within the limits of the tests, by the combined effects of temperature and electric stress.

(*c*) Cables examined after more than 20 years of operation are still in perfect condition and will go on operating satisfactorily for many more years if present loading conditions are maintained.

It is suggested that the mechanical deterioration of paper should be taken as a criterion of the state of used cables and to fix, for both new and used cables, the temperature limits for normal and emergency loading in relation to the desired life of the cable.

An Oscillating Synchronous Linear Machine. Paper No. 3351 U.

E. R. Laithwaite, M.Sc., Ph.D., and R. S. Mamak, B.Sc.

The paper describes the development of a new type of a.c. generator in which the moving member travels between a d.c. pole structure in a straight line with reciprocating motion. The induced e.m.f. appears in the moving member, which consists of single loop conducting material which embraces and moves along the core. The core carries stationary coils which experience an induced alternating e.m.f. by transformer action from the moving loop. These coils are not associated with the air-gap between the d.c. poles and are more easily cooled than the windings of a rotary machine. The loop, which is confined to the air-gap, can be run at high temperature, since it carries no insulation. The machine can be used as a generator to convert mechanical power supplied directly from a piston. It can also be used as a synchronous motor for such purposes as the driving of compressors without cranks; when used in this manner the machine is self-starting. Essentially a single-phase machine, the equivalent of multi-polar rotary machines can be constructed which will generate e.m.f.'s at 50 c/s with mechanical oscillations at frequencies lower than 3 000 per minute.

The construction of the machine is simple, since the core is made up entirely from rectangular stampings, while the windings consist of four transformer-type coils and a conducting loop.

An experimental machine is described, development of which consisted largely of testing devices to minimize the internal impedance. Test results are given.

The paper includes a proposed design for a 33 kVA generator.

Silicon Power Rectifiers. Paper No. 3362 U.

A. J. Blundell, A. E. Garside, B.Sc.(Eng.), R. G. Hibberd, B.Sc., and I. Williams, B.Sc.

The silicon rectifier is now well established over a wide range of voltages and currents, and, in all probability, will remain as a standard class for many years to come.

The paper opens with a brief survey of the processes involved in the preparation of single-crystal silicon; this is followed by sections devoted to design considerations and process techniques used in the preparation of silicon rectifier cells.

The electrical characteristics and ratings of rectifier cells, and the considerations involved in their operation in rectifying equipments, are discussed in some detail; brief mention is made of the various fields of application in which silicon rectifiers will offer advantage.

The latest device of this class—the silicon controlled rectifier—is described, and its importance in the future is emphasized.

Several theoretical aspects of the forward and reverse characteristics of silicon rectifier cells are treated in the Appendices.

A General Theory of Depreciation of Engineering Plant. Paper No. 3366 S.

D. Rudd, B.Sc.(Eng.).

The conventional methods of providing for the depreciation of engineering plant are criticized on the grounds that they contain arbitrary features, and a general theory is formulated which is not subject to such criticism. The general theory is applied first to individual projects, showing how the factors which affect depreciation operate, and secondly to an integrated industry, showing how the programmes for writing off the investments in the industry can be co-ordinated and the economic assessment of new projects facilitated in consequence. The effects of variations in the value of money are also considered.

The propositions are illustrated by numerical examples drawn, in the case of the application to an integrated industry, from the field of public electricity supply.

Some Notes on the Electrical Requirements of General Cargo Docks. Paper No. 3365 U.

E. R. Radway.

The paper, which is written with special reference to the South Wales ports, outlines the function of a general cargo dock and the effect of grouping such docks into a port system; its object is the stimulation of thought and discussion on the improvement of electrical engineering in the dock industry—an industry where efficiency can affect the living standards of everyone in Britain.

The paper illustrates and comments on the electrical supply and distribution practices of the industry, the electrical features of the pumping plant used for hydraulic power production and impounding services, the requirements for tenants and the safety of shipping, and it discusses some of the problems associated with the mechanical-handling plant.

Passenger reception and handling peculiar to the liner terminal ports, and the electrical installations of ships, are outside the scope of the paper.

Quantitative Treatment of Three-Phase Brush-Shifting Series Commutator Motor. Monograph No. 413 U.

O. E. Mainer, M.Sc.(Tech.).

Previous quantitative treatments appear to have ignored the loss component of motor current. In this paper an approximate equivalent circuit, which makes allowance for this component, is developed from first principles. A method is devised for correcting the errors introduced by the approximate treatment so as to obtain an accurate solution. The average percentage errors in motor current, power factor and input obtained by both approximate and accurate treatments are given for a wide range of operating conditions.

The Surge Corona Discharge. Monograph No. 415 S.

R. Davis, M.Sc., and R. W. E. Cook.

Exploratory experiments are described followed by an account of a more systematic study of the corona discharge with concentric-cylinder electrodes. From experimental records which relate the charge flow in an external circuit to the applied voltage, the corona current and energy loss were derived. An attempt is made to interpret the observations in terms of modern views on the mechanism of electrical breakdown in gases. The attenuation by corona of surges on transmission lines is examined in an Appendix.

Temperature Rises in Electrical Machines with Sustained Variations in Load and Speed. Monograph No. 416 U.

B. J. PRIGMORE, M.A., M.Sc.(Eng.).

A method is presented for obtaining the temperature-rise/time curve for a given machine for an arbitrary sequence of operating currents and speeds; the method is demonstrated to give results for such a run correct to about $\pm 2\,^{\circ}C$ in 60–70°C.

The method is to suppose that the equivalent thermal network of the machine is linear, its temperature/time curve thus being the sum of the temperature/time transient responses for a series of short times, δt, successive transients corresponding to the average operating conditions during successive intervals; and then to modify this curve to that for the actual non-linear machine by adding a correction curve which is itself composed of two series of transient responses: one of these allows for the effects of non-linearity due to temperature rise, and is based upon the succession of average temperatures, given from the first curve, during the intervals δt; the other allows for the effects of changes in dissipation coefficients due to changes in speed.

The test-bed procedure for obtaining the temperature/time transients for the linear machine, and the corrections for non-linearity, is specified. It is recommended that this procedure, lasting about 36 hours, should be applied to samples of appropriate types of machine.

Numerical Evaluation of Inductance and A.C. Resistance, with particular reference to Electrical Machines. Monograph No. 418 U.

R. S. MAMAK, B.Sc., and E. R. LAITHWAITE, M.Sc., Ph.D.

The electric and magnetic circuits of electrical machines are generally so complex that the exact evaluation of such quantities as leakage reactance and a.c. resistance is virtually impossible. With the advent of digital computers it has become feasible to develop numerical methods of predetermining the flux pattern in such cases. In the paper the finite-difference equations for electromagnetic systems are obtained, and the inductance is calculated by integrating the magnetic vector potential over conducting surfaces. The same finite-difference equations are applied to the calculation of a.c. resistance of conductors in slots. The use of the method is illustrated by examples of standard transformers, a tap-changing transformer, calculation of the leakage reactance of salient-pole-alternator windings and the screening of d.c. poles in a new type of oscillating synchronous linear machine.

Limitations of Distance-Type Protective Equipment when Applied to Long Extremely-High-Voltage Power Lines. Monograph No. 421 S.

A. WRIGHT, M.Sc.

On power lines the currents which flow solely in the phase conductors travel near the velocity of light whereas those which return through the ground and overhead earth wires travel more slowly, the speed depending on the line spacings, conductor sizes and nature of the ground. The full steady-state equations for a 3-phase line are developed and a particular line is examined to show the magnitude of this effect. The effect of transposing a line is also indicated.

The steady-state performance of basic distance protection fitted with compensating equipment to allow for the effects of the currents in the sound phases during earth faults is studied, using the line equations. It is shown that large errors of measurement occur under earth-fault conditions on long lines fed from small sources. The performance for faults which do not involve earth is satisfactory.

It is shown that non-transposition of a line makes the apparent impedance of a fault on a line depend on the source impedance. This causes further errors in the assessment of the positions of faults of all types.

The high transient time-constants on long lines increase the chance of maloperation during the transient period.

It is concluded that distance protection may not be satisfactory on very long lines and that a study must be made of the complete power system before a decision can be made about its suitability for any particular application.

Interconnected Rotor Induction Motors. Monograph No. 422 U.

N. C. ENSLIN, Ph.D.

The stators of two identical wound-rotor induction motors are connected to a common supply to produce fields revolving in the same direction. The shafts are coupled mechanically and the rotor circuits are electrically connected together with the voltages initially phased so that no rotor current flows. By rotating one stator relative to the other through an angle β the rotor voltages are no longer in anti-phase, rotor currents flow and the unit produces torque.

Boucherot adopted a similar scheme in the construction of a composite machine with two stators and a common squirrel-cage rotor. The mid-points of the rotor bars were connected together through a resistive element so that currents always flowed in the rotor circuits and zero torque could not be obtained.

Expressions for torque are derived for the shunt-interconnected rotor developed by Boucherot and for the purely series-interconnected motor investigated by the author. It is shown that, for the former, torque cannot be controlled from zero without voltage variation, whereas in the latter arrangement zero torque is always obtained when the phases of the induced rotor voltages are in opposition.

The properties and applications of the series interconnected motor circuit are discussed. By the insertion of impedance the torque/speed characteristics of the motors can be modified retaining the zero torque position. Possible applications include small hoists, cranes or lifts, and positioning, manipulating or tensioning drives for which precise control is required.

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ABBREVIATIONS

(P)—Address, lecture or paper.
(D)—Discussion.

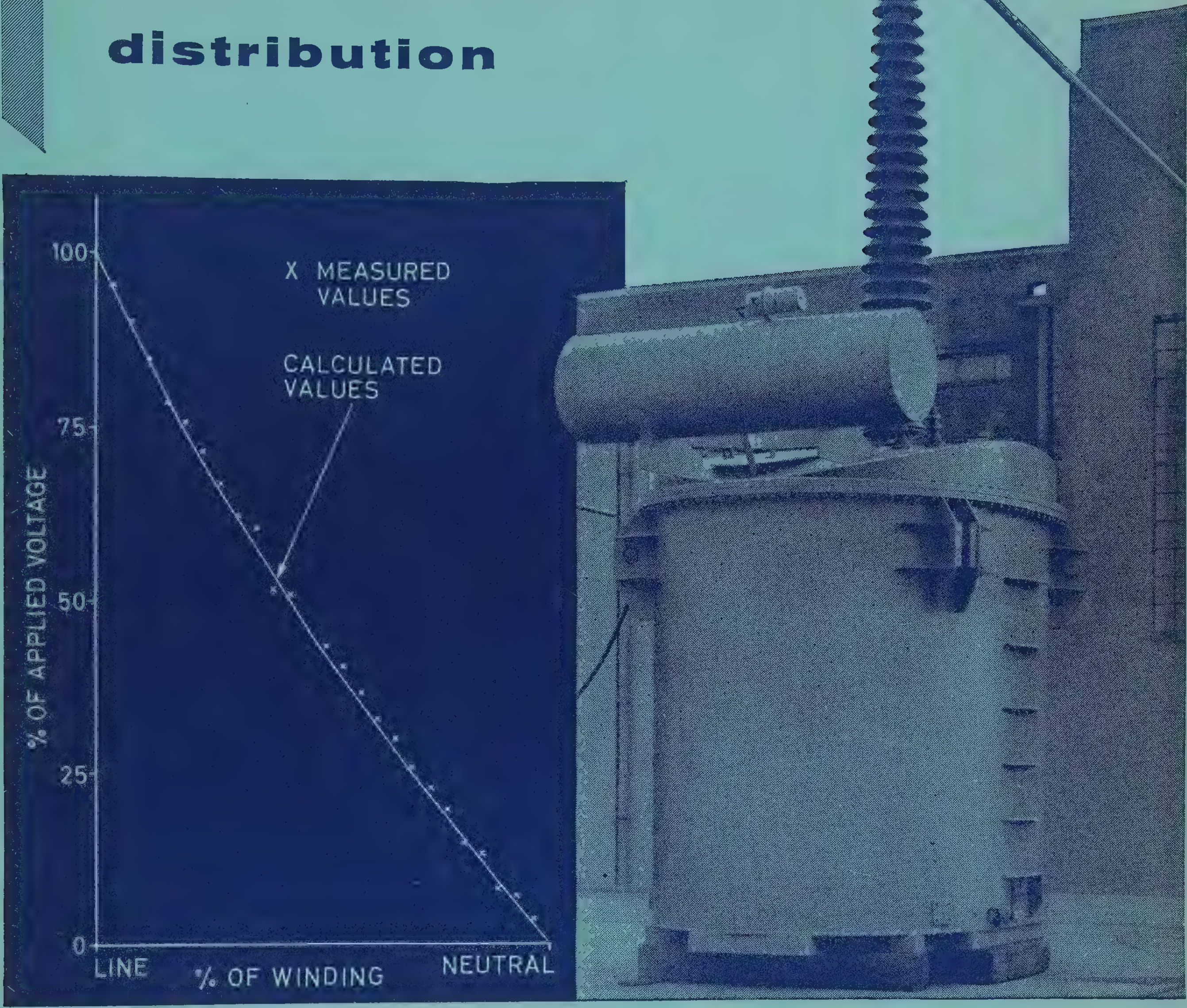
E

F

G

UNWIN BROTHERS LIMITED, WOKING AND LONDON

Calculated surge voltage distribution



'English Electric' patented interleaved windings enable the transformer designer to calculate the stress distribution throughout the whole winding under impulse voltage conditions, thus ensuring that the correct insulation is built into the windings. The graph above illustrates the accurate relationship between the measured and calculated surge voltage distribution for a 500 kV single phase transformer.

'ENGLISH ELECTRIC'

transformers

The ENGLISH ELECTRIC Company Limited, English Electric House, Strand, London, W.C.2

Transformer Department, Stafford

WORKS: STAFFORD · PRESTON · RUGBY · BRADFORD · LIVERPOOL · ACCRINGTON

TFS.31A

PROCEEDINGS OF THE INSTITUTION OF ELECTRICAL ENGINEERS

Part A. POWER ENGINEERING, DECEMBER 1960

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